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A HIGH TEMPERATURE ALLOY DIE CASTING PROCESS

D. J. McMillin
General Electric Company

Technical Report AFML-TR-69-223
August 1969

[redacted]

MANUFACTURING TECHNOLOGY DIVISION
AIR FORCE MATERIALS LABORATORY
AIR FORCE SYSTEMS COMMAND
WRIGHT-PATTERSON AIR FORCE BASE, OHIO

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A HIGH TEMPERATURE ALLOY
DIE CASTING PROCESS

D. J. McMillin
General Electric Company
Lamp Metals and Components Department

[redacted]

FOREWORD

This final report covers the work performed under Contract F33615-68-C-1190 from 1 December 1967 through 31 March 1969. It was released by the author on 15 June 1969 for publication.

This contract with General Electric Company, Lamp Metals and Components Department, Cleveland, Ohio, was initiated under Manufacturing Methods Project 167-8. It was administered under the technical direction of Mr. R. L. Kennard of the Materials Processing Branch (MATP), Manufacturing Technology Division, Air Force Materials Laboratory, Wright-Patterson Air Force Base, Ohio 45433.

Parts of the program effort reported here were conducted by the Doehler Jarvis Division of the National Lead Company, Toledo, Ohio; the Dort Metallurgical Company, a Division of the Moline Malleable Iron Company, St. Charles, Illinois; the General Electric Company Research and Development Center, Schenectady, New York; and the Lamp Metals and Components Department of the General Electric Company, Cleveland, Ohio. In a supportive role, the Manufacturing Engineering Services Operation of the General Electric Company, Schenectady, New York, produced precision cast prototypes.

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This project has been accomplished as a part of the Air Force Manufacturing Methods Program, the primary object of which is to implement, on a timely basis, manufacturing processes, techniques, and equipment for use in economical production of USAF materials and components. The program encompasses the following technical areas:

Metallurgy	- Rolling, Forging, Extruding, Drawing, Casting, Powder Metallurgy, Composites
Chemical	- Propellants, Plastics, Textile Fibers, Graphite, Fluids and Lubricants, Elastomers, Ceramics

Electronic - Solid State, Materials and Special Techniques, Thermionics

Fabrication - Forming, Material Removal, Joining, Components

Suggestions concerning additional Manufacturing Methods projects required on this or other subjects will be appreciated.

This technical report has been reviewed and is approved.

George M Glenn
for H. A. JOHNSON
Chief, Materials Processing Branch
Manufacturing Technology Division

ABSTRACT

To demonstrate that a device in the form of a hemisphere could be produced by ferrous die casting at a lower cost than by aluminum die casting, the desirability and feasibility of automatically transferring molten ferrous alloys to an injection system was explored. The requirements of an injection system were established; materials of construction and design concepts were selected and tested for both vertical and horizontal die casting machines. New die design concepts were explored. Operational results are reported. The structures and properties of several die cast ferrous alloys were characterized, and 16 potential die materials were evaluated. Prototypes were fabricated, from which the final material and configuration for the hemispheres could be selected. A die was designed and fabricated for pilot production, and the results of that pilot casting operation are reported. The cost of the pilot castings was analyzed and potential costs were projected. Recommendations are made for additional support.

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SECTION I

INTRODUCTION

1. OBJECTIVE

The objective of this project was to adapt the die casting process to the production of sound, dimensionally accurate castings from ferrous alloys of high melting point, at a cost below that possible employing other casting techniques and metalworking processes.

Die casting is a process in which molten metal is injected, under pressure, into a split, metallic, permanent mold. It is this injection pressure that distinguishes die casting from permanent mold casting; and it is this injection pressure, too, that overcomes the restrictions imposed by the fluidity of the cast metal on gravity casting processes, i.e., die casting can more faithfully reproduce detail and more successfully produce intricate, thin-walled castings than any other casting process. The metallic dies and the intimate contact, produced between the cast metal and the die by the injection pressure, affect an extraordinarily rapid solidification and cooling rate that also characterizes the die casting process. This rapid solidification and cooling rate results in a refinement of the cast structure relative to the structures found in castings produced by other processes. Generally speaking, such a structural refinement can be expected to result in increased strength, ductility, and toughness, and in a more rapid response to heat treatment.

The die casting process is also notable for the unusual productivity that can be attained for a modest investment and a minimum commitment of floor space. This felicitous situation is the result of the high cycle rates of which the process is capable.

Although this project was restricted to a consideration of a process suitable for die casting ferrous alloys, it is anticipated that the conclusions reached should be generally applicable to alloys having liquidus temperatures in the range of 900°C to 1600°C. Among the common engineering materials in this category are cobalt, copper, iron, nickel, and their respective alloys. With specialized melting and metal transfer techniques, the process may one day find application to the production of chromium and titanium alloy parts, too.

2. CHALLENGE

The inherent economic and technical advantages of the die casting process, as outlined above, have made the die casting of such materials as lead, tin, zinc, magnesium, and aluminum common practice in industry for years. These materials all have liquidus temperatures below 660°C.

Brasses, with liquidus temperatures in the 900°C range, are also commercially die cast, but the brass die casting process has not received the wide acceptance accorded the die casting of those metals having lower liquidus temperatures. For example, nearly 99% of zinc castings are die cast. The ratio is 50% to 60% for aluminum, 40% to 50% for magnesium, but only approximately 3% for copper alloys.

Commercial die casting, until very recently, did not exist for alloys having liquidus temperatures higher than those of brass. The reason is simple. Other things being equal, ultimate die lives become shorter as metals and alloys with higher liquidus and pouring temperatures are cast. Die fabrication costs, which may be quite large, must be amortized over the life of the die. At some limiting die life, therefore, the per part cost of die fabrication becomes so great that die casting becomes economically unattractive. For example, the dies used to die cast zinc alloys, which have liquidus temperatures of approximately 390°C, seldom require maintenance in less than 100,000 cycles, and may not require maintenance in less than 1,000,000 cycles. For aluminum alloys, which commonly have liquidus temperatures in the 580°C to 630°C range, 100,000 cycles is a reasonable expectancy for die life. Dies used to die cast brass, with liquidus temperatures in the 920°C to 1000°C range, may require maintenance after 10,000 cycles; and this work has demonstrated that H-13 dies, used to die cast 304 stainless steel with a pouring temperature of 1500°C to 1600°C, will require maintenance in less than 1,000 cycles.

The event that dramatically improved the prospect for the establishment of an economically viable ferrous die casting industry was the announcement on 12 May 1966, that the General Electric Company had successfully demonstrated the feasibility of die casting ferrous alloys in molybdenum dies. The challenge posed by this project was to demonstrate that, by making those adaptations necessary, the die casting process could be made an economically practical technique for the production of high quality, precision iron and steel castings for a specific USAF component.

3. ORGANIZATION

To accomplish the objective of the project in the shortest possible time, responsibility for the in-depth exploration of the several critical elements of the ferrous die casting process was divided among four major participating organizations. This, perforce, meant that each participant had to establish an independent ferrous die casting facility; and even though close technical liaison was maintained between the four groups, each was permitted a great deal of technical independence. Although this approach resulted in some redundancy, it also resulted in some remarkably different solutions to the same challenge.

This report is organized to present an exposition of the work related to each of the critical elements of the ferrous die casting operation in turn. The pilot production phase of the project, which represented the culmination of the exploratory effort, is then discussed, followed by a cost study, conclusions, and recommendations for future work.

SECTION II

LIQUID METAL TRANSFER

Throughout the project, Doehler Jarvis, the Dort Metallurgical Company, and the Lamp Metals and Components Department transferred molten ferrous metal from melting or holding crucibles to the pressure injection systems of their respective die casting machines by hand, in small ladles or spoons. The Research and Development Center, however, employed a 200-ton Morton Vertacast (vertical) die casting machine in their work, which incorporated a vacuum lift device for automatically transferring molten metal to the pressure injection system.*

1. MANUAL TRANSFER

Hand ladling is extremely common, if not prevalent, in the aluminum and brass die casting industries, where materials limitations generally preclude the use of the hot chamber die casting machines that are so common in zinc die casting. (In hot chamber die casting, a suitably modified pressure injection system, sometimes called a gooseneck, is immersed in the melt with an orientation such that the plunger stroke is nearly vertical. The hydraulic injection cylinder is suspended above the melt.) Although hand ladling may be considered the ultimate in simplicity, there are problems associated with the technique--some of which are common to aluminum and brass die casting, and some of which are unique to ferrous die casting.

Hand ladling requires the molten bath to be continuously or, at best, intermittently exposed to the atmosphere. This promotes oxidation, which may result in significant changes in chemical analysis and significant material losses; and it inevitably leads to the formation of slag or dross, which must be carefully removed from the surface of the melt to avoid forming slag inclusions in the castings. The high melting temperatures of ferrous alloys make it impractical to heat ladles to temperatures above, or even close to, the melt temperature. This has several ramifications: Skulls are formed in the ladles, which represent a loss of material. Labor time is required to remove these skulls and prepare the ladle for further use. Careless preparation or use of metal ladles may permit the molten ferrous metal to melt through the bottom of the ladle or to become welded into the ladle, effectively destroying it. (Whereas nonmetallic ladles are safe from this risk, they are usually quite fragile.) To compensate

*The Vertacast machine is now manufactured by Press Automation Systems, Detroit, Michigan, a division of U. S. Industries, Incorporated.

for the temperature drop resulting from heat loss to the ladles, bath temperatures must be maintained at somewhat higher levels than would otherwise be desirable. This hastens oxidation of the bath and increases the risk of melting through or welding to the ladles with the results already noted.

To make the best of this difficult situation, Doehler Jarvis' first approach was to pour from the melting furnace into small, preheated clay graphite crucibles, which were then used to transfer the metal to the shot sleeve. The crucibles employed had been specially cut down for that purpose. That approach was found to be too time consuming, and it was forsaken. In its place, Doehler adopted preheated stainless steel ladles which could be dipped directly into the melt, if they were adequately protected. To achieve the required protection, the ladles were coated with a ceramic, which was applied by dipping them into an aqueous suspension of 20 volumes of kaolin, 3 volumes of sodium silicate, and 20 volumes of water. The coating was fired at approximately 1400°F. Near the end of their participation in the project, Doehler Jarvis discovered that the life of the stainless steel ladles could be extended substantially by coating them with a 1/8" blanket of WRP-X felt impregnated and cemented in place with a mixture of kaolin and waterglass (sodium silicate).*

Preheated plumbago (clay graphite) ladles were also employed by Dort during most of their participation in the project.** Initially, like Doehler Jarvis, Dort poured molten metal from the melting crucible into red-hot plumbago ladles. Later, they too realized that the cycle time of the operation could be reduced by dipping the ladles into the melt. Unlike Doehler Jarvis, Dort continued to use clay graphite ladles after resorting to dipping. Qualitatively, Dort observed that much less ladle skull was formed in clay graphite ladles than in ceramic-coated, malleable iron ladles. As will be noted in Section VIII of this report, however, Dort was unable to obtain plumbago ladles for the pilot production phase of this project, and was forced to resort to preheated malleable iron ladles coated with Ironton-Berlite Wash.***

---

*WRP-X felt is a product of the Refractory Products Company, Evanston, Illinois.

**Although plumbago ladles are available from several other sources, those employed by Dort in this project were all supplied by Casting Materials Company, White Plains, New York.

***Ironton-Berlite cement is manufactured by the North American Refractories Company, Cleveland, Ohio. It is a Kyanite (Al_2SiO_5) base material, with graphite added to reduce wetting and slag attack. In this application, it was diluted and used as a wash.

The Lamp Metals and Components Department employed preheated malleable iron ladles throughout the die casting campaign related to this contract. Three ladle coatings were investigated. Arco Perm 100, a siliceous-base material with bonding and suspending agents added, was applied as a slurry by dipping.* In retrospect, it would probably have been more compatible with the acid refractories that can be used in iron melting practice than with the MgO refractories used to melt the stainless steel which was being cast. A water suspension of vermiculite (magnesium silicate), known as EP 3450,** was adopted next as a ladle coating, and it became the standard ladle coating for the duration of the project--although acetylene black was also investigated near the end of the campaign. The acetylene black proved to be a better release agent for the inevitable ladle skulls than the ceramics, and it appeared to offer adequate protection for the ladles if properly applied. The satisfactory application of acetylene soot, however, seemed to require too much time and attention to be really practical; and the coating did not prove to be as durable as the ceramic coatings.

2. AUTOMATIC TRANSFER

Automatic transfer devices generally fall into three categories:

a. Mechanical

b. Gravity-feed

c. Pneumatic.

There are many possible variations on the theme of mechanical, automatic transfer (e.g., one device that captures the imagination is the robot, which may be trained to perform by rote). Mechanical, automatic transfer devices generally suffer many of the shortcomings of manual transfer. Furthermore, a mechanical device cannot discriminate, as a man can, between slag and metal, for example. Their chief virtue is that they act as an indefatigable, low-cost replacement for the human operator.

Gravity-feed devices deliver the required quantity of liquid metal by unstoppering a nozzle or orifice for a programmed time interval and then restoppering it. Inasmuch as they are bottom feed devices, they eliminate concern over trapped slag and make the introduction of an inert gas atmosphere easy. To achieve gravity feeding, the holding crucible must be located above the level of the shot sleeve. Although troughs or launders might be used to transfer the metal from the device to the shot sleeve, that practice might also introduce skull losses and excessive oxidation.

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*Arco Perm is supplied by the Arcoa Corporation, Toledo, Ohio, as a plastic or putty-like material requiring dilution with water.

**EP 3450 is manufactured by Foseco, Incorporated, Cleveland, Ohio.

Positioning such a device so that it will deliver its metal directly into the shot sleeve, on the other hand, poses difficult design problems, inasmuch as the dimensional constraints imposed by the die casting machine and the holding furnace are not entirely compatible. Such an arrangement also puts a premium on the reliability of the system. Failure to successfully restopper the device would delay operations and might well create conditions hazardous to operating personnel.

Pneumatic devices for automatic metal transfer use gas pressure to lift the metal to the level of the shot sleeve. In this respect, they are more nearly fail-safe than the gravity-feed devices. Like the gravity-feed devices, they deliver metal from the bottom, or close to the bottom, of the holding crucible, either through a transfer tube or a built-in "teapot spout," thus avoiding slag entrapment. Like the gravity-feed devices, too, they permit an inert gas atmosphere to be maintained over the melt.

The original intention was to evaluate two alternate pneumatic devices for the automatic transfer of molten ferrous metals to the pressure injection system of a die casting machine. One was a hyperbaric device, and the other was a vacuum lift device. Both had been successful for aluminum die casting.

Unfortunately, however, the manufacturer of the hyperbaric device was unable to solve the materials problem posed by the high melting temperatures of ferrous alloys within the time allotted for this project. More specifically, satisfactory refractories were not identified; and those used in the first prototype device failed, making it impossible to reproducibly control the weight of metal delivered. The Dort Metallurgical Company was to have evaluated the hyperbaric device in service.

As previously noted, the 200 ton Morton vertical die casting machine, employed by the Research and Development Center, incorporated a vacuum lift system for molten metal transfer. The principle employed by this system, and all other pneumatic systems, is to affect transfer by establishing a pressure differential between the top and the bottom of the transfer tube or spout. Whereas such a pressure differential is created in a hyperbaric device by pressurizing the melting or holding furnace, in a vacuum lift device, the differential is created by evacuating the air from the die cavities, the shot sleeve, and the transfer tube, and establishing a partial vacuum. Atmospheric pressure, acting on the surface of the melt, then forces metal to flow up the transfer tube and into the shot sleeve. The height to which the metal might ultimately rise under equilibrium conditions, disregarding freezing, is a function of the pressure differential and the density of the metal. In a Morton Vertacast machine, however, the system is not permitted to approach even near-equilibrium conditions. The pressure differential is employed only to establish flow of the liquid metal. The rate of flow is determined by the geometry of the system, and the volume of metal transferred is determined by controlling the duration of the flow. Flow is terminated by advancing the plunger. This effectively blocks the metal entrance port. Air at atmospheric

pressure is admitted into the bottom of the shot sleeve behind the plunger. After the plunger passes the metal entrance port, there ceases to be a pressure differential between the top and bottom of the transfer tube, and the column of metal in the tube flows back into the furnace.

The establishment of a partial vacuum in such a system dictates the generous use of o-rings. In the Research and Development Center system, a large o-ring was located in the parting plane. That o-ring encircled the entire die. Similar o-rings were located between the A plate and the "top" clamping plate (the lower plate, in this case), between the B plate and the rigid insulating board that separates the B plate from the support plate, and between the insulating board and the support plate.* Each ejector pin traveled up and down through a gland containing an o-ring. Other o-rings formed a seal between the shot sleeve and the "top" clamping plate, between the lower end of the shot sleeve and the shot sleeve liner, and between the plunger in its lower, or retracted position, and the shot sleeve. Figure 1 illustrates these and other details of construction.

For aluminum die casting, evacuation of the air from the die cavities, shot sleeve, and transfer tube of a Morton Vertacast machine is conventionally accomplished by pumping on a vacuum channel that lies in the parting plane of the die encircling all of the die cavities. The cavities are connected to that channel by overflow gates, and the channel normally becomes filled with aluminum during injection. This limits the a priori metal yield to a relatively low figure. Aluminum is prevented from entering the vacuum lines, which communicate with the vacuum channel, by advancing close fitting pins into the vacuum line openings at the same time the plunger is actuated. To improve metal yield, the Research and Development Center modified the evacuation system on their Morton machine. Evacuation of the die was accomplished by pumping on a circumferential groove in the cavity of the B plate. That groove communicated through radial grooves in the heater plate with openings in the back face of the ejector half die insert in juxtaposition with each ejector pin. When the dies were closed initially, the ejector pins were retracted 1/2" from the die face into what was essentially a vacuum manifold, permitting evacuation through the ejector pin holes. The intersections of the vacuum manifold with the ejector pin holes were, by design, subtended by an angle of less than 180°. The holes, therefore, provide positive guidance to the pins throughout their stroke. As the plunger began to advance, the ejector pins moved forward, preventing molten metal from entering the evacuation channels. Figure 2 clearly illustrates the features described. As in the standard Morton system, a second small mechanical vacuum pump was connected to concentric annular grooves in the shot sleeve and the shot sleeve liner, just above the o-ring that forms a seal between the plunger and the shot sleeve liner. The purpose of that pump was to compensate for vacuum leaks in the lower end of the injection system.

*D-M-E nomenclature is used throughout this report.

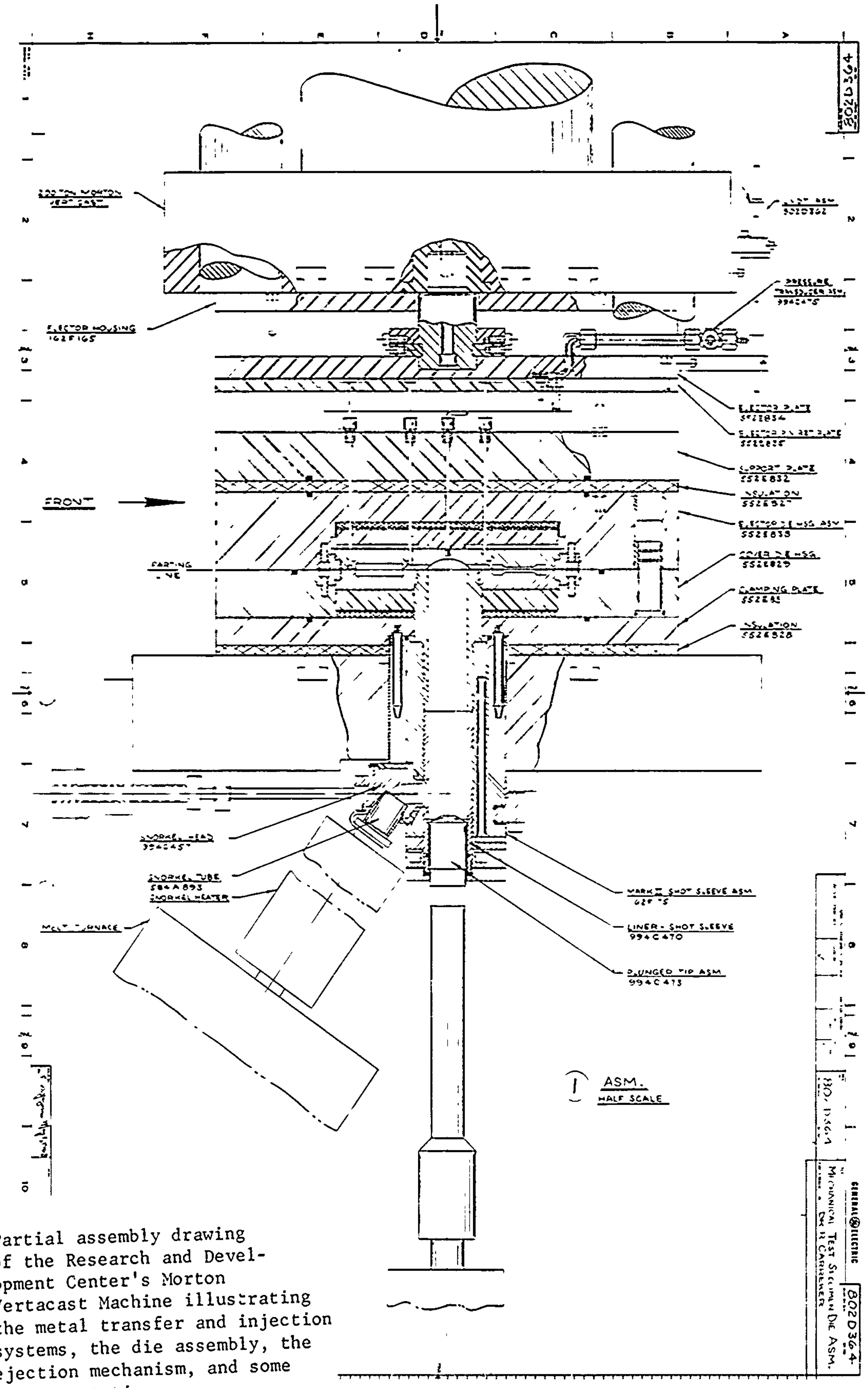

Figure 1 - Partial assembly drawing of the Research and Development Center's Morton Vertacast Machine illustrating the metal transfer and injection systems, the die assembly, the ejection mechanism, and some instrumentation.

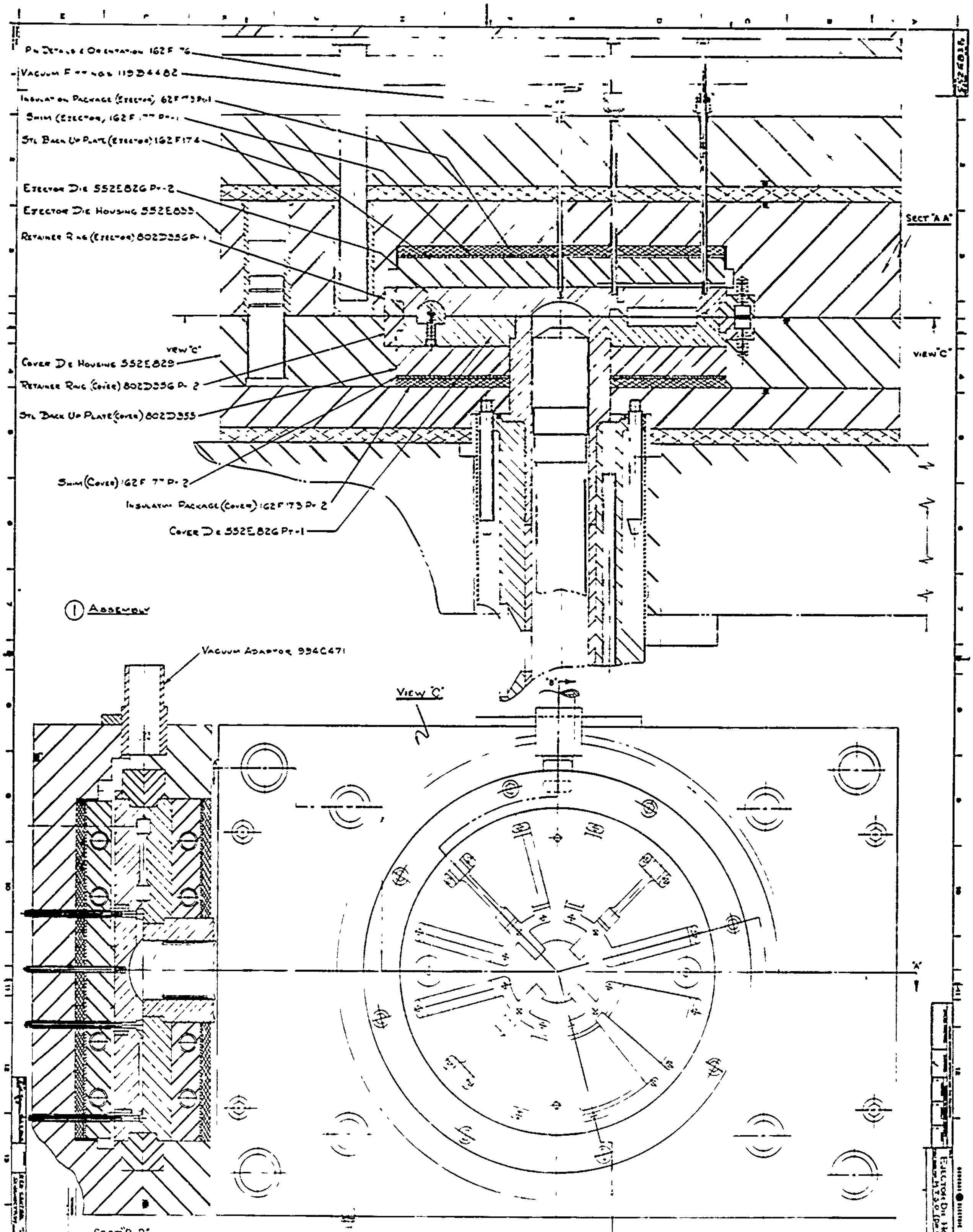

Figure 2 - Face view of the ejector die and sectional views of the complete Research Center test bar die.

For aluminum die casting, Morton supplies cast iron transfer tubes lined with Fibrefrax paper.* These tubes are preheated in the beginning of the run only. Subsequently, reliance is placed on the sensible heat of successive shots to maintain the temperature of the transfer tube. Because the melting points of many ferrous metals exceed that of cast iron, other materials had to be identified for the construction of a transfer tube suitable for ferrous die casting. Refractory ceramics seemed the logical choice.

Several problem areas were anticipated:

a. The tubes had to resist chemical attack by molten metal and slag

b. The tubes had to resist thermal shock

c. Provision had to be made to prevent freezing in the transfer tube.

The transfer tube, which first evolved from the efforts of the Research and Development Center to overcome the problems identified, was a composite in the form of a graphite tube, clad inside and out with Fibrefrax felt impregnated with Ceramobond, an alumina-base, refractory cement.** The composite tube was quite strong and resistant to thermal shock. The Fibrefrax adequately protected the graphite from oxidation and was not chemically attacked by molten cast iron or its slag (although it was attacked by some of the nickel-base alloys investigated in work unrelated to this contract). Although the insulating value of the Fibrefrax was expected to be of great assistance in preventing freezing in the transfer tube, further insurance against freezing was provided by constructing a resistance-wound, split tube furnace, which was intended to heat the transfer tube above the melt line.

The transfer tube was coupled to the shot sleeve by an elbow fabricated from Masrock, a fused, granular silica.*** The Masrock elbow was jacketed in a stainless steel shell and lined with Fibrefrax, cemented in place with Ceramobond. The lining prevented the molten metal from reacting with or impregnating the Masrock. The stainless steel shell, which was relatively strong and gas tight, was secured against a conical seal in the shot sleeve by a push rod driven by a hand screw. The tapered end of the transfer tube was cemented into the elbow with Ceramobond and mechanically fastened with a clamp that connected the steel shell to a flange on the tube. These provisions proved to be sufficient to attain a rough vacuum seal. Details of the split tube furnace and the complete transfer tube assembly are illustrated in Figure 3.

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*Fibrefrax is the trade name for a family of products manufactured from aluminum silicate fibers by the Carborundum Company, Niagara Falls, New York.

**Ceramobond is produced by Aremco Products, Briarcliff Manor, New York.

***Masrock is produced by Glassrock Products, Incorporated, Atlanta, Georgia.

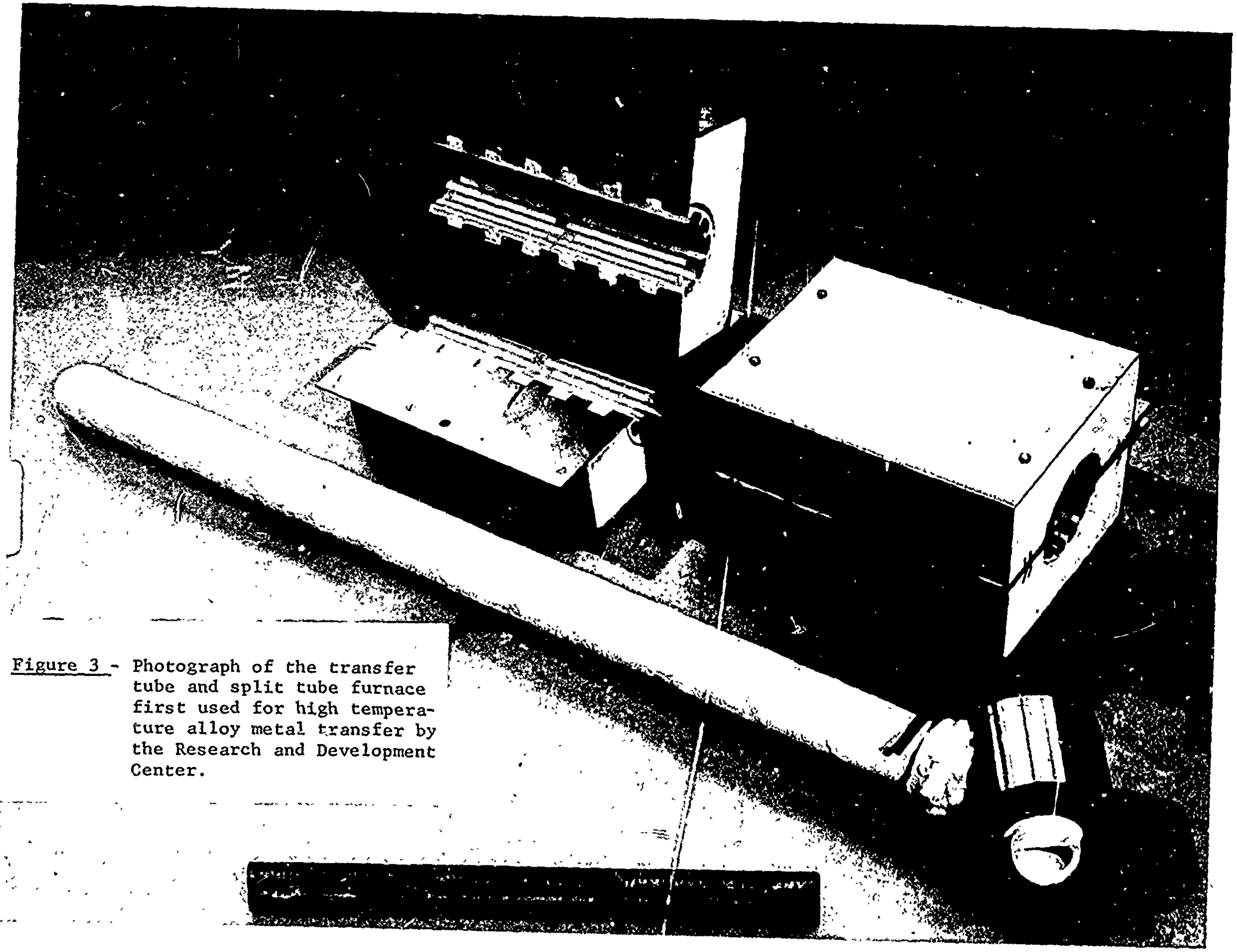

Figure 3 - Photograph of the transfer tube and split tube furnace first used for high temperature alloy metal transfer by the Research and Development Center.

Although the transfer tube described above performed reasonably well, experience revealed that metal was freezing in the elbow coupling. Figure 4 is an example of the problem. The progressive restriction of the channel through the transfer tube progressively reduced the rate of liquid metal flow through the tube and progressively increased the transfer time required. If a point of equilibrium were reached under such conditions, the operation might proceed; but automatic control prior to the establishment of equilibrium would be nearly impossible. If equilibrium were not achieved, the operation would be terminated. The latter result was actually observed.

After the system was put into operation, it was also discovered that not all of the liquid metal in the transfer tube returned to the melt when the plunger passed the metal entrance port. Some inevitably spilled into the shot sleeve behind the advancing plunger. If the spilled metal adhered to the bore of the shot sleeve, it interfered with retraction of the plunger. The same result could be observed when some of the liquid metal adhered to the shot sleeve end of the coupling as sessile drops or a meniscus and extended into the shot sleeve. The latter situation also often resulted in either the transfer tube, or the coupling, or both being broken when the plunger was retracted.

Intuitively, it was felt that the geometry of the system, incorporating the elbow coupling with its horizontal leg, restrained drainage of the liquid metal back into the melt, and thus promoted spillage. It was felt, too, that the situation was aggravated by the freezing of liquid metal in the coupling. When freezing occurred, the horizontal segment of the elbow coupling, in effect, became lined with solidified metal. That lining, however, became progressively thinner at points closer to the entrance port. The net result was to create a situation in which the originally horizontal segment of the coupling, in effect, sloped downward into the shot sleeve, thereby promoting the flow of metal into the shot sleeve. (The solidified metal lining may also have established a superior substrate from which sessile drops might extend into the bore of the shot sleeve.)

An initial attempt was made to improve drainage from the elbow coupling back into the melt. The coupling was hand shaped to eliminate the horizontal segment. The experiment was a success. Encouraged by that success, the Research and Development Center redesigned the transfer-tube, shot sleeve system. As illustrated in Figure 5, the elbow coupling with its horizontal leg was eliminated; the transfer tube was connected directly to the shot sleeve at an acute angle; the split tube furnace was replaced by an induction coil which used the graphite core of the composite transfer tube as a susceptor; and the Fibrefrax felt cladding previously used in the transfer tube was replaced by preformed tubes of porous alumina impregnated with Ceramobond. (The last change made the transfer tubes much easier to fabricate and increased their resistance to chemical attack.)

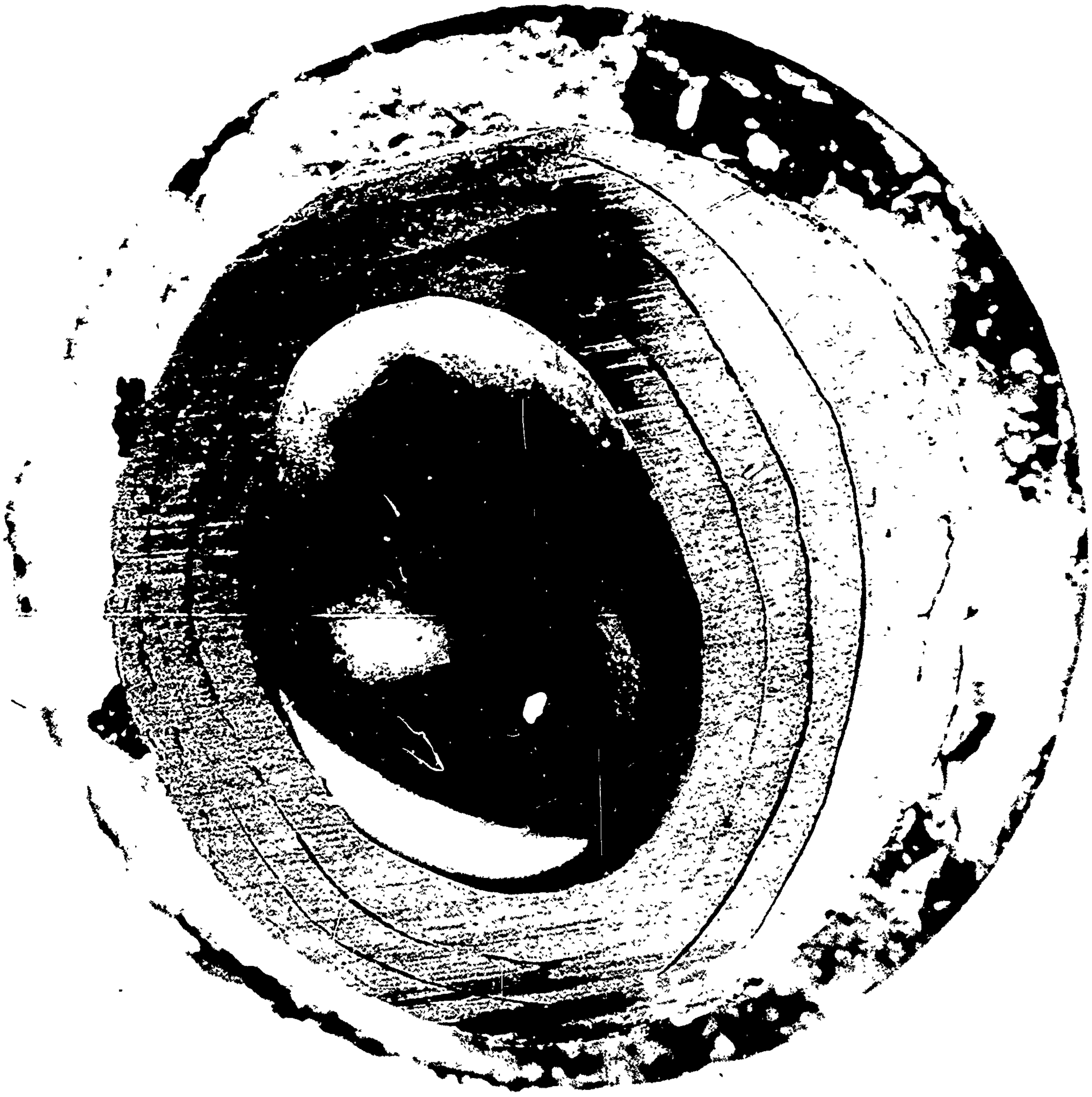

Figure 4 - Photograph of a transverse section through a transfer tube used by the Research and Development Center. Note the progressive restriction of the passage by successive shots.

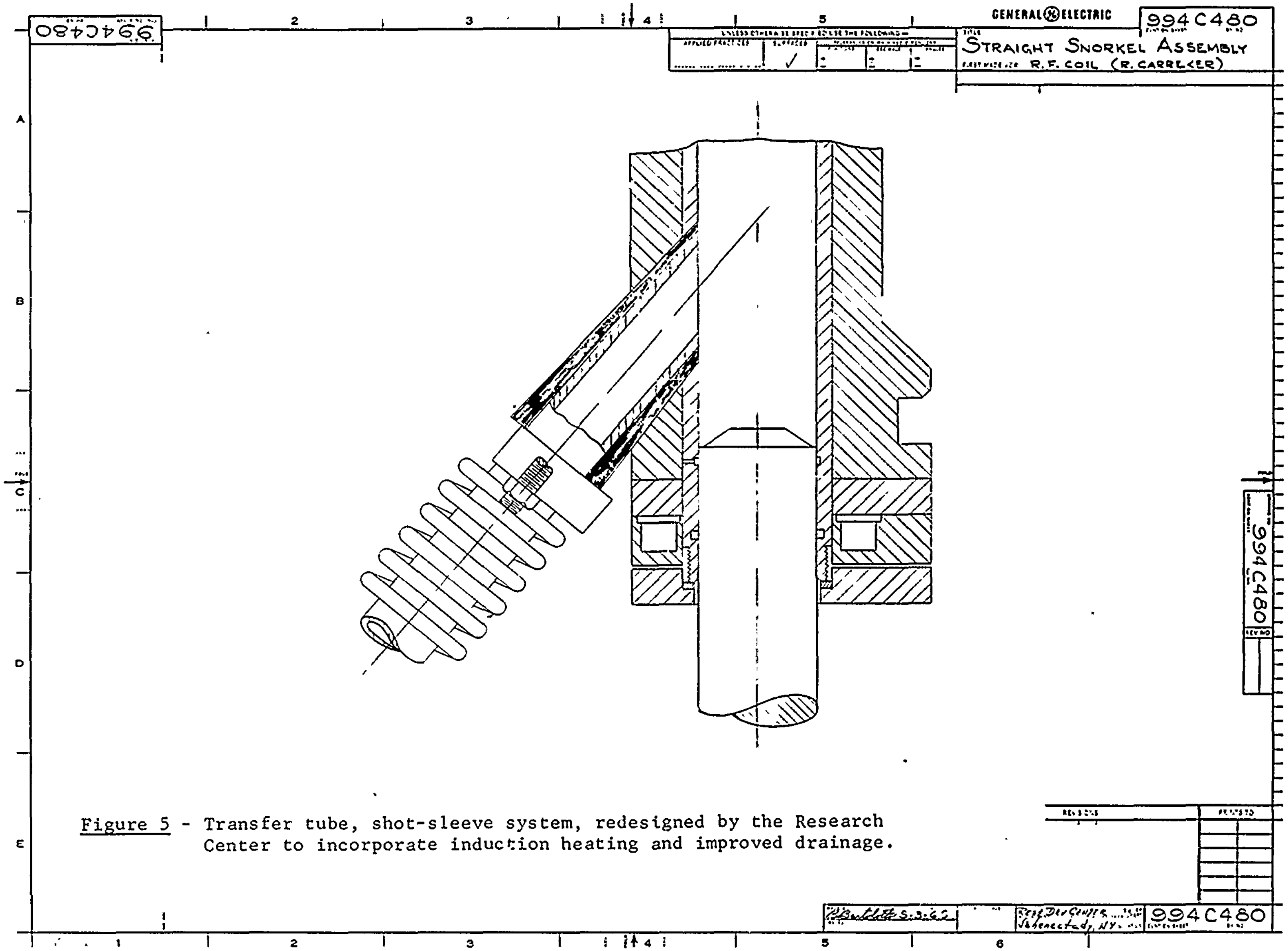

Figure 5 - Transfer tube, shot-sleeve system, redesigned by the Research Center to incorporate induction heating and improved drainage.

The induction heating was a success. The problem of freezing in the transfer tube was eliminated. Those problems related to improper drainage, however, seemed, if anything, to have been made worse. In the first run, after having accomplished the desired modifications, excessive spillage was encountered. The transfer tube broke, and the plunger seized in the shot sleeve. Subsequently, the Research and Development Center employed an induction heated metal transfer tube fabricated from concentric tubes of alumina, graphite, alumina with an elbow coupling at the shot sleeve. No freezing was encountered, either in the tube or in the coupling, but liquid metal was frequently discovered to have spilled into the shot sleeve after the plunger had passed the metal entrance port.

Using General Electric Company funds, the Research and Development Center built a full-scale plexiglass model of the transfer tube, shot sleeve, and plunger. Using water as a working fluid, high speed motion pictures were taken to observe the dynamics of the filling action as the fluid traveled upward, first under the influence of vacuum, and then propelled by the plunger. The water traveled up the transfer tube as a compact mass with no significant turbulence at its top surface. The mass traveled across the shot sleeve as a compact jet, breaking up only after it struck the opposite wall. Thereafter, the shot sleeve filled in a highly turbulent manner. The plunger advanced and blocked the flow up the transfer tube. Ambient air pressure reached the transfer tube opening as the plunger continued to advance, and the water fell back down the tube under the influence of gravity, the portion along the bottom edge of the tube moving first. A small amount of water, however, was observed to spill into the shot sleeve, just as it had been surmised that liquid metal did during injection. The movie graphically illustrated the direct impingement of the stream on the shot sleeve wall opposite the transfer tube opening, the nonturbulent passage of metal up the transfer tube, and the highly turbulent manner in which the shot sleeve filled. It was concluded that, within the geometric restrictions of the existing system, a slower rate of fill would be desirable to reduce the velocity of the rising stream of metal. The velocity might also be reduced at a constant mass flow rate by increasing the effective cross-sectional area of the transfer tube. A reduction of velocity is desirable to reduce the turbulence in the shot sleeve and the related opportunity for gas entrapment and to reduce the probability of "washing out" the shot sleeve opposite the metal entrance port. (Pour hole "wash out" is normally the net result of thermal fatigue cracking and erosion.) The obvious disadvantage of reducing the rate of fill (i.e., the mass transfer rate) is that there must be a concomitant increase in casting cycle time.

The persistence of porosity in the castings produced by the Research and Development Center and the size, shape, and distribution of the porosity defects observed, finally dictated that serious consideration be given to the possibility that gas was being entrapped in the castings. Because the time actually required for metal transfer was short (< 0.5 seconds), because a crude vacuum gage located in an evacuation channel in the die registered less than 25" of mercury during evacuation, and because excellent filling of fine detail was achieved in the die, evacuation had not initially been recognized as a problem. When the approximately 40:1 compression ratio of the injection system was considered, however, it became obvious that any inadequacy in the evacuation system could result in significant volumes of entrapped gas.

Direct measurement of the conditions in the die during evacuation and filling would have been impossibly difficult. The times involved were very short (e.g., 0.5 seconds to affect transfer, 0.25 seconds to affect injection), and the environment in which a gage would be forced to perform was extremely hostile (i.e., a die being filled with high-pressure molten metal). As a practical alternative, therefore, an effort was undertaken to calculate the conditions existing in the system during evacuation, transfer, and injection.

Using funds provided by the General Electric Company, Dr. I. Edelfelt, of the Research and Development Center, prepared a computer program to predict, as a function of time, both the pressure in the system and the location of the upper surface of the rising column of liquid metal. The program included the following variables:

a. Mold volume (including cavities, shot sleeve, and transfer tube)

b. Transfer tube geometry (diameter, length, angle, submerged length, bottom orifice)

c. Shot cylinder diameter

d. Melt properties (density, viscosity, pressure)

e. Evacuation channel data (number, cross-section geometry, length)

f. Temperature of die

g. Die to air heat transfer (zero or infinite)

h. Pump characteristic (pumping speed versus pressure)

i. Leakage into die

j. Surge tank volume.

Typical results of the analysis are presented in Table I.

Table I--Computer Analysis of the Vacuum Lift System for Automatic Metal Transfer

	Model Number						
Conditions	1	2	3	4	5	6	7
Shot sleeve diameter (in)	2.5	2.0	2.0	2.0	2.0	2.0	2.0
Surge tank volume (in^3)	600	600	600	600	50	600	50
Evacuation channel diameter (A<B<C)	A	B	B	C	C	C	C
Transfer tube diameter	0.625	0.875	0.875	0.875	0.875	0.875	0.875
Orifice diameter (in)	0.625	0.875	0.375	0.375	0.375	0.375	0.375
Leak area (in^2)	0	0	0	0	0	0.010	0.010
Results							
Melt enters shot sleeve-- Elapsed time T_1 (sec)	0.52	0.33	0.7	0.6	0.6	0.7	0.7
Transfer complete--* Elapsed time T_2 (sec)	0.95	0.50	1.3	1.0	1.0	1.0	1.0
Pressure at T_1 (psia)	5.2	1.4	0.33	0.23	<0.005	1.3	1.3
Pressure at T_2 (psia)	1.9	0.7	0.1	0.07	<0.005	1.2	1.2

*Based on transferring 7 in^3 of molten metal

The evacuation-filling analysis clearly illustrates several points. Over half the total fill time is occupied by travel of the metal up the transfer tube before it enters the shot sleeve. If adequately large evacuation channels are provided (Cases B and C instead of A), pressures well below 1 psia can be realized in times of the order of one second, providing the system is tight. It would be desirable to remove the 600 cubic inch surge tank that was used prior to this analysis and connect the vacuum pump directly to the die. (The pump corresponds to a surge tank volume of 50 cubic inches.) If the system were tight, pressures below 0.01 psia could be achieved. Using a restricted orifice at the bottom of the transfer tube (e.g., a 3/8" orifice in a 7/8" tube) would effectively increase the time available for evacuation by slowing down the fill rate. However, the most important point is that leakage corresponding to a 0.010 square inch orifice is sufficient to override all such effects, keeping the pressure level above 1 psia, even though such leakage has only a small effect on the filling time.

The computer analysis focused attention on the leak rate of the die and the injection and transfer systems. Although it would have been difficult to make leak measurements during an actual shot, it was feasible to make such measurements during simulated shots with a cold die. A suitable pressure gage, capable of high-speed response, was obtained and mounted over a cavity in a lucite plate which was clamped to the face of the ejector die. An o-ring seal was provided between the lucite plate and the die, and the vacuum pump was connected directly to the ejector die through an adaptor. The pressure was observed to drop to virtually its ultimate value in approximately 0.5 seconds, but the ultimate pressure was no lower than 6.9" of mercury or 3.4 psia. When the vacuum pump was valved off, the pressure in the die increased rapidly. Obviously, the ejector die leaked badly. (Similar measurements were not made on the cover die and injection system, but there is little reason to suspect that they will be significantly more vacuum-tight than the ejector die.) An attempt was made to identify the leak paths in the ejector die by successively sealing suspected paths with RTV silicone rubber.* The worst leak paths were associated with the Transite plate used as thermal insulation between the B plate the the support plate.**

Although it is possible, in principle, to construct a die that can be successfully evacuated to very low pressures in very short times (as indicated by the computer analysis), the experimental data clearly indicate the die fabricated by the Research and Development Center for this project did not meet that standard of performance. The vacuums that were attained, however, created pressure differentials that were more than adequate to

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*RTV is manufactured by the General Electric Company, Silicone Products Department, Waterford, New York.

**Transite is a cement-bonded asbestos board produced by Johns-Manville, New York, New York.

affect metal transfer. Once the concept of using a vacuum to create a pressure differential to affect metal transfer has been divorced from the concept of vacuum die casting, constructive thinking about die design and operational procedure can begin. (For example, it is obvious, in retrospect, that the Research and Development Center die should have incorporated provision for venting.)

The accomplishments of the Research and Development Center, related to automatic, liquid metal transfer, were first to demonstrate the practicality of the vacuum lift system for ferrous die casting. Equally important was the evolution of a successful transfer tube and a technique for keeping it free from solidified metal. Although the problem of metal spilling into the shot sleeve was not solved, it was demonstrated to be fundamentally a problem associated with the vertical orientation of the shot sleeve. This leads one to suggest incorporating the vacuum lift system of metal transfer in a horizontal die casting machine. If the metal entrance port were located in the bottom of a horizontal shot sleeve, it would be physically impossible for the molten metal to spill into the shot sleeve after the pressure differential used to affect transfer had been relieved. Such a system is presently being employed for the cold chamber die casting of aluminum by an operating department of the General Electric Company. The induction-heated, composite transfer tube could obviously be used equally well in either the vacuum lift system or in the hyperbaric type of pneumatically activated automatic transfer device.

Although a later section will be devoted to economic analysis, it is useful to note at this point that the use of such a device by the Dort Metallurgical Company for the pilot production phase of this project would have reduced their transfer time from 6 seconds to a maximum of 1.3 seconds, raising their production of good castings (two hemispheres per casting) from 98 to 115 per hour. It would have eliminated the consumption of ladles and the cost of preparing them, and it would have eliminated metal lost in handling. On that basis, Dort could have realized a gross saving of 9.21 cents per shot in their pilot production run, had an automatic transfer device similar to the vacuum lift system been available to them.

SECTION III

PRESSURE INJECTION

As indicated in the Introduction, it is the pressure used to inject molten metal into a die that distinguishes die casting from permanent mold casting. In the "cold chamber" process employed in this program, the liquid metal transfer and the pressure injection are normally separate and distinct operations. (Although when the "vacuum lift" system of metal transfer is employed, that separation becomes much less distinct.) The principle employed is to displace the molten metal which has been transferred to the shot sleeve into the die by advancing a plunger connected to a hydraulic cylinder.

Maintaining trouble-free operation in a pressure injection system becomes an even greater challenge as metals with higher pouring temperatures are die cast; and, like the dies, those components of the injection system exposed to the molten metal tend to exhibit shorter lifetimes as metals with progressively higher pouring temperatures are die cast. In recognition of the adverse effect that the necessity of frequently repairing or replacing elements of the pressure injection system would have on the economics of ferrous die casting, a major effort was devoted to producing a reliable, long-lived system.

In a horizontal die casting machine, the injection system is horizontal: the platen and die movement is horizontal; and the parting faces of the dies are predominantly vertical. In a hand ladling operation, molten metal is charged through a hole in the top of the shot sleeve near its plunger end. The metal impinges on the bottom of the sleeve, directly beneath the pour hole, and partially fills the sleeve. Activating the injection system causes the plunger to advance, raising the level of the liquid metal until it first fills the shot sleeve and then is injected into the die. By proper controlling the shot weight, the plunger is permitted to advance past the pour hole before the molten metal fills the shot sleeve.

In a vertical die casting machine, the injection system and the relative movement of the platens and dies are vertical; the parting faces of the dies are predominantly horizontal. In a hand ladling operation, the molten metal is poured into the open mouth of the shot sleeve, onto the face of the plunger, through the open dies. The dies are then closed and the plunger advanced, filling the dies. By carefully controlling the shot weight, the shot sleeve can initially be filled very nearly to the parting face of the die. This creates a vastly different thermal environment than that in the shot sleeve of a horizontal die casting machine.

As already noted, in a hand-ladled vertical die casting machine, the dies cannot be closed until the metal is transferred. The die closing time must then be added tc the time the molten metal resides in the shot sleeve. This is an important consideration in the design of an injection system for a vertical machine.

The die closing time of a hand-ladled vertical machine must also be added to the overall cycle time. Metal spilled on the die face of a vertical machine can prevent the die from locking up properly, creating a safety hazard and, perhaps, causing the casting to be scrapped; it can ruin a die; or it might only delay operations. These considerations, among others, have made vertical die casting machines generally less popular than horizontal machines.

Because of the substantial differences that exist between the requirements placed upon pressure injection systems in horizontal and vertical machines used to die cast ferrous alloys, systems for the two machine configurations were explored independently. The greater emphasis was placed on a system for horizontal machines, partly because they are more widely used, but also because the injection system for the horizontal machine poses the more difficult challenge.

1. HORIZONTAL MACHINE

A. Approach

Both the Dort Metallurgical Company and the Lamp Metals and Components Department used horizontal die casting machines to perform the ferrous die casting necessary to accomplish their individual program assignments.

Dort was assigned responsibility for devising a workable pressure injection system and demonstrating its value. Dort's approach was to first survey the literature for promising materials of construction, and then to characterize the thermal environment created in the pressure injection system of a horizontal machine by ferrous die casting. The thermal information generated was then used to design alternate systems which were evaluated in simulated service.

Even when ferrous die casting was in its infancy, the necessity of achieving a long-lived pressure injection system for the process was obvious, but it was also obvious that other aspects of the process were more crucial. To permit its attention to be directed to those more crucial problems, the Lamp Metals and Components Department first had to devise a workable pressure injection system, which could be quickly, easily, and inexpensively replaced or repaired when difficulties were encountered.

As-drawn steel tubing was selected as a suitably low-priced commodity to serve as a shot sleeve liner, but the steel liner required projection from molten ferrous alloys. One scheme that was evaluated was to form an expendable lining of refractory paper or fabric that could be slipped into the

shot sleeve through the face of the open die. That approach was subsequently abandoned in favor of a thick coating of Foseco EP 3450 (a suspension of vermiculite in water) that was sprayed into the sleeve. Not only did that coating insulate the steel liners from the melt and protect them from thermal shock and soldering, but it also augmented the seal between the plunger tip and shot sleeve liner, permitting an unconventionally large clearance to be maintained between the two.

The vermiculite coating was recognized as a potential source of gas, but it was the time required to apply the spray that prompted the Lamp Metals and Components Department to seek alternate techniques for protecting shot sleeve liners. Acetylene black was substituted for vermiculite; no detrimental effects were noted. Thereafter, the shot sleeve, like the die, was coated only with acetylene black.

After evaluating several softer materials as plungers, the Lamp Metals and Components Department settled on nitrided Nitralloy 135 (Ryerson's trade name for MIL-S 6709, 0.30% - 0.43% carbon, 0.50% - 0.70% manganese, 0.20% - 0.40% silicon, 1.4% - 1.8% chromium, 0.95% - 1.30% aluminum, 0.30% - 0.40% molybdenum, balance iron).

B. Materials

Three classes of materials were considered by Dort for application as shot sleeves (or shot sleeve liners) and plungers: metals, ceramics, and composites. Extensive consideration was also given to refractory coatings for metals and composites. The prime consideration, regardless of the classification into which the materials fell, was that they be able to withstand the thermal shock to which they would be exposed.

A number of metals having otherwise excellent properties were ruled out as being deficient in thermal shock resistance. Other properties considered important or pertinent to the performance of metals in the pressure injection system at the time were:

a. Good creep resistance

b. High stress-rupture strength

c. High short-time, elevated temperature mechanical properties

d. High thermal conductivity

e. Good oxidation resistance.

For shot sleeve materials, the value of these properties at the maximum service temperature of the shot sleeve was considered to be the important value. For plungers, which may be water cooled, the room temperature properties may be more important.

Of the metals surveyed, the following alloys were considered most promising for application as shot sleeves:

a. Haynes Number R-41 (Rene' 41)--Nickel-base alloy; solution heat-treated 4 hours at 1950°F, AC; aged 16 hours at 1400°F, AC[1]*

b. L-605--Cobalt-base alloy; solution treated at 2250°F and rapidly air cooled[2]

c. Waspalloy--Nickel-base, precipitation hardening alloy; solution treated at 1975°F for 4 hours, AC; stabilized at 1550°F for 24 hours, AC[3]

d. TZM-Molybdenum--Stress relieved[4]

e. Inconel 625--Nickel-chromium alloy; mill-annealed bar.[5]**

The 1000 hour stress rupture strength, the 0.2% offset yield strength, the percent elongation, and the thermal conductivity of the five selected alloys, each as a function of temperature, are displayed in Figures 6 through 9, respectively.

Although the oxidation resistance of TZM (a molybdenum-base alloy containing 0.5% titanium, 0.08% zirconium, and 0.03% carbon) is not outstanding, this alloy had performed very well as a die material for the die casting of brass.[6] The other alloys selected possess excellent resistance to oxidation and scaling at high temperatures.

Inconel 625 exhibits resistance to scaling at 1800°F. For Waspalloy, the proven resistance to oxidation is satisfactory through 1600°F. L-605 has good oxidation resistance for intermittent service at temperatures up to 1900°F and for continuous service up to 1800°F. Rene' 41 is successfully used for afterburner parts and hardware exposed to elevated temperatures in jet engines.

Of the five alloys selected, three appeared to be outstanding prospects for the construction of shot sleeves for high-temperature alloy die casting. The three are TZM, Rene' 41, and Waspalloy.

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*René-41 is a trademark of Teledyne, Incorporated. The machinability of this alloy in the solution annealed condition can be improved by water quenching.

**Inconel 625 is a product of the Huntington Alloy Products Division of the International Nickel Company, Huntington, West Virginia.

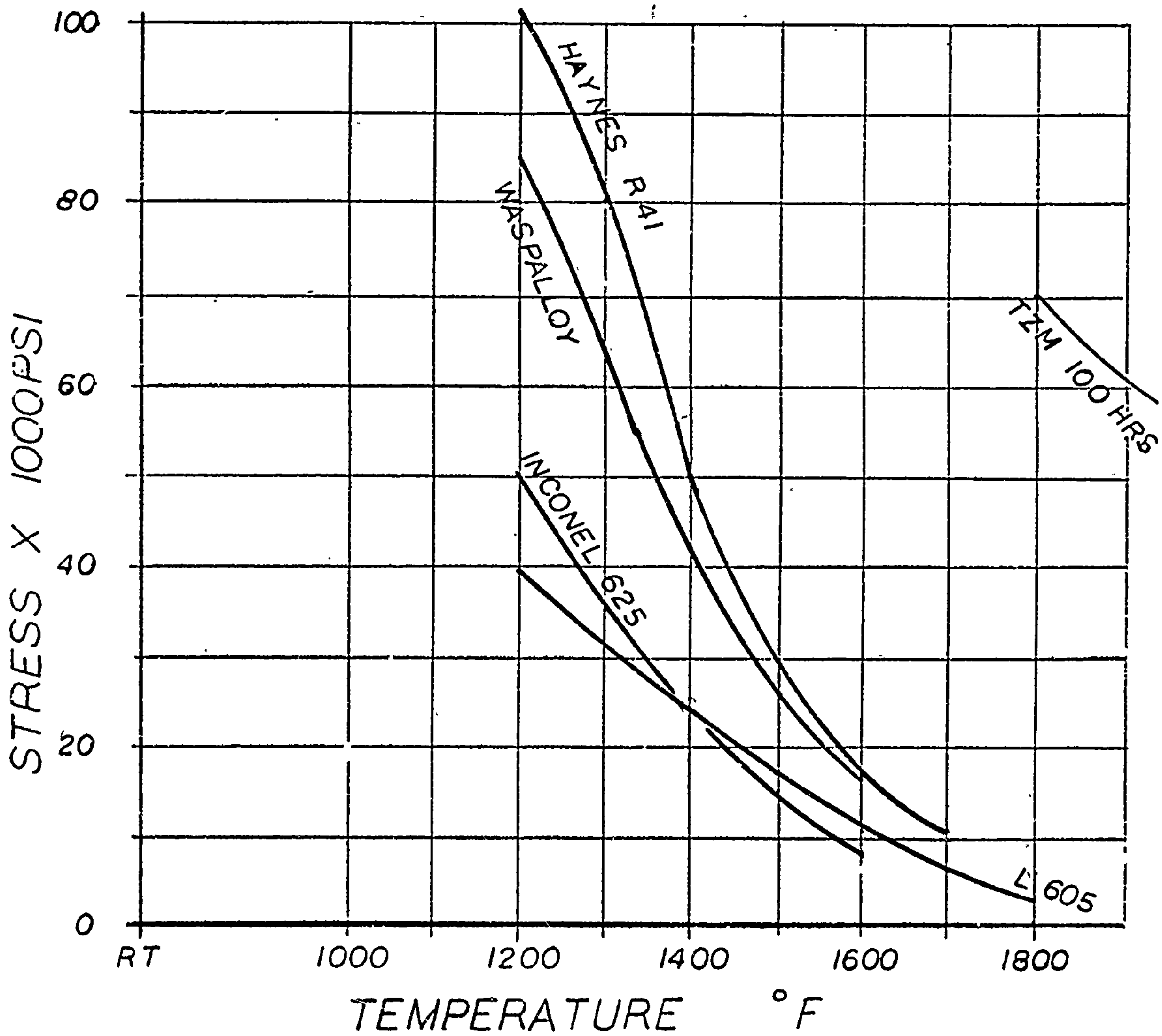

Figure 6 - 1000 hour stress rupture strength of five selected alloys as function of temperature.

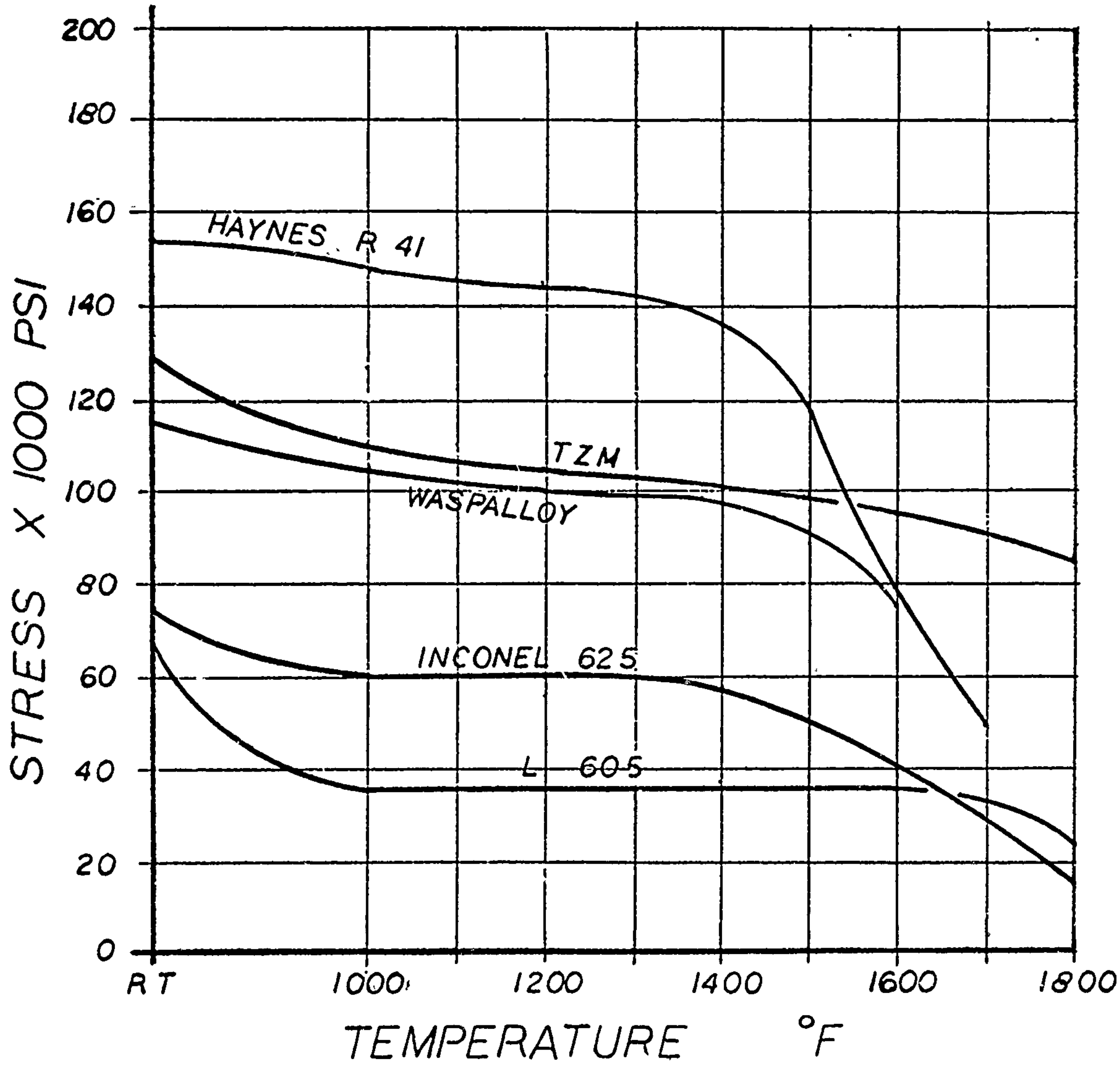

Figure 7 - 0.2% yield strength of five selected alloys as function of temperature.

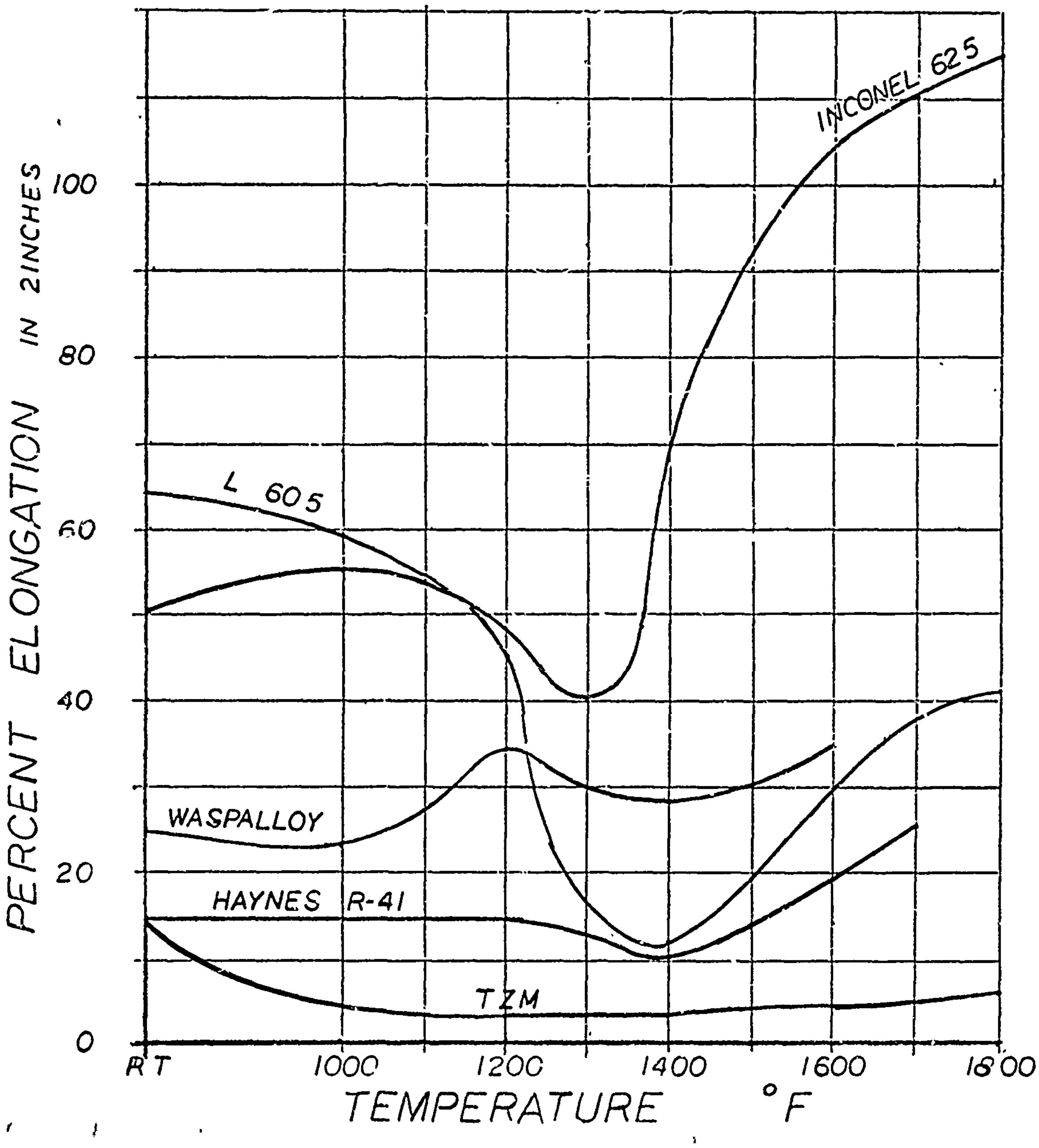

Figure 8 - Tensile elongation of five selected alloys as a function of temperature.

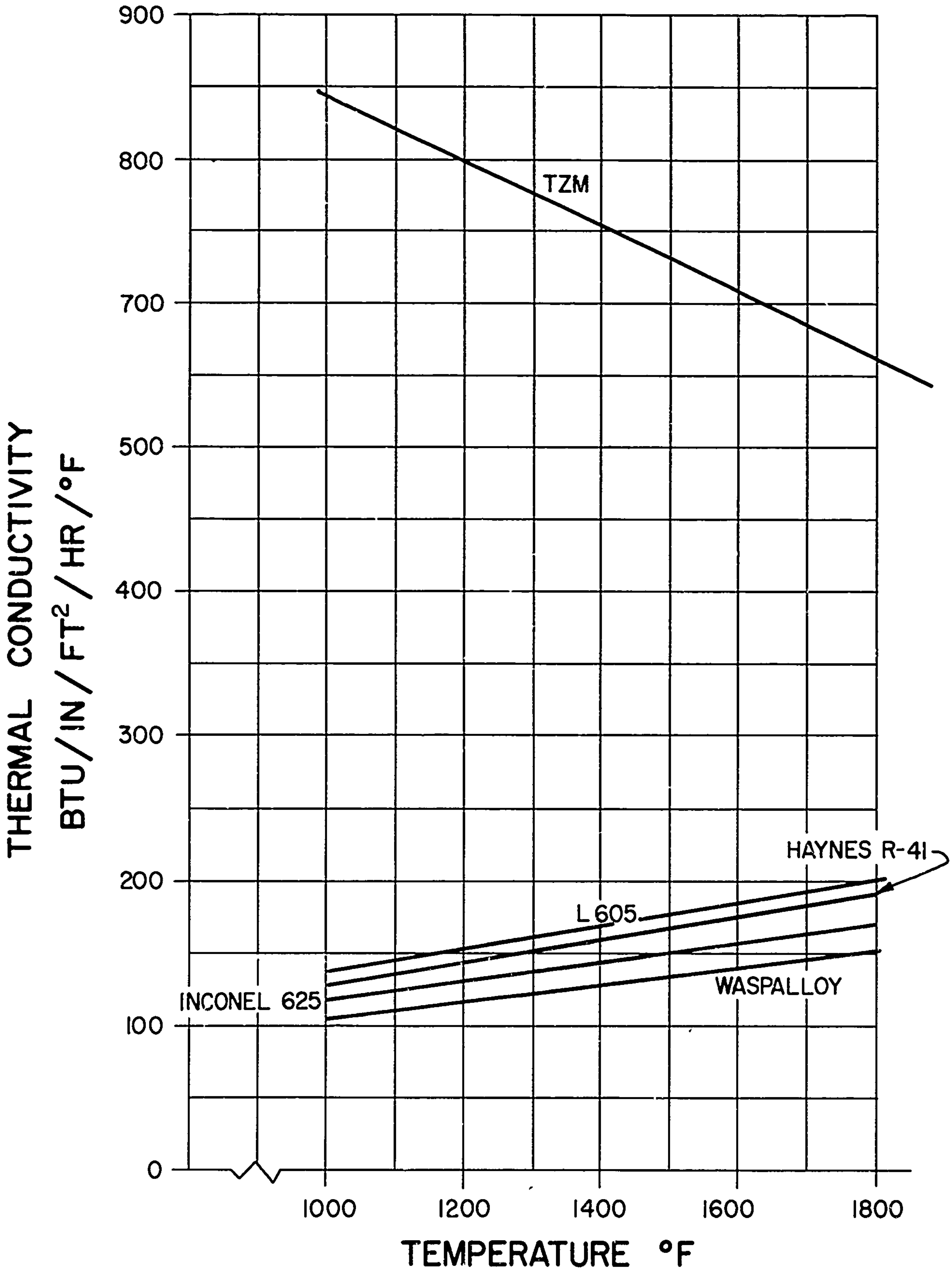

Figure 9 - Thermal conductivity of five selected alloys as a function of temperature.

Beryllium copper, which has become virtually the standard plunger material in the aluminum die casting industry, was used throughout the program by Dort. (The specific alloy employed was the Beryllium Corporation's Berylco 10, 0.40% to 0.70% beryllium, 2.35% to 2.70% cobalt, balance copper, aged for three hours at 900°F.) Some consideration was also given to plunger materials exhibiting a lower coefficient of thermal expansion, and the possibility of employing either Invar or TZM in the capacity of a plunger was explored. That work will be discussed later in this report.

The anticipated rigorous thermal, chemical, and mechanical environment of the shot sleeve severely limits the number of candidate ceramic materials. To be successful, it was felt that a material had to exhibit adequate mechanical strength at 2600°F to 2900°F, have good abrasion resistance, resist the corrosive attack of molten ferrous alloys, show resistance to thermal shock and steep temperature gradients, and have moderate oxidation resistance.

The more promising commercial ceramic materials are listed in Table II, along with a number of pertinent properties. Many other refractory materials were omitted because of their experimental nature, limited availability, expense, or because they showed limited promise for this application. Among the more commonly used refractories, silicon carbide (SiC) was excluded on account of its known reactivity with molten ferrous alloys; and synthetic graphite was ruled out because of its relatively poor abrasion and oxidation resistance, as well as its moderate solubility in molten iron.

Many of the ceramic oxides meet all of the above requirements, with the exception of thermal-shock resistance. The one oxide which does have excellent resistance to thermal shock, beryllium oxide (BeO), was excluded from consideration because of the toxicity problem. Of the materials listed in Table II, mullite and zircon are probably best for this application, due primarily to their relatively low thermal expansivity.

However, if a ceramic sleeve can be preheated or continuously heated externally, the thermal shock problem would be virtually eliminated. Most ceramic materials are capable of withstanding moderate thermal shocks and thermal gradients when their temperature is maintained above 1800°F. Inasmuch as mullite or zircon sleeves would probably also require preheating to 1600°F to 1800°F to avoid serious deterioration from thermal shock, their advantage over the single oxides is minimal.

Table II--Properties of Some Selected, Commercial, Refractory Oxides

Material & Formula	Melting Point °C	Limit of Usefulness in Oxidizing Atmospheres °C	Hardness Moh's Scale	Hardness Vickers (100 g)	Hardness Knoop (100 g)	Linear Coefficient of Thermal Expansion (E) $cm \cdot cm^{-1} \cdot °C^{-1}$ Temp °C	$E \times 10^7$
Alumina-Al_2O_3	2015	1950	9	2700	2020	20-1580	80
Beryllia-BeO	2550	2400	9	--	1300	20-1400	95
Calcia-CaO	2600	2400	4.5	--	560	20-1200	136
Magnesia-MgO	2800	2400	6	--	--	20-1400	140
Silica-SiO_2 (crystalline)	1728	1680	7	1200	--	20- 300 300-1100	430 30
Silica (vitreous)-SiO_2	--	990*	--	--	--	20-1250	5
Thoria-ThO_2	3300	2700	7	--	945	20-1400	95
Zirconia-ZrO_2 (stabilized)	2600	2500	7-8	--	1208	20-1200	55
Mullite-$3Al_2O_3 \cdot 2SiO_2$	1830	1800	6-7	--	--	20-1320	45
Spinel-$MgO \cdot Al_2O_3$	2110	1900	8	--	--	20-1250	90
Zircon-$ZrO_2 \cdot SiO_2$	2420	1870	7.5	--	--	20-1200	55

*Strain point

Table II--Properties of Some Selected, Commercial, Refractory Oxides

(Continued)

Material & Formula	Thermal Conductivity (K) $cal \cdot sec^{-1} \cdot cm^{-2} \cdot cm \cdot °C \cdot ^{-1}$ Temp °C	Porosity %	$K \times 10^4$	Thermal Shock Resistance	Chemical Compatibility: Reducing Atmosphere	Carbon	Acid Slags	Basic Slags	Metals
Alumina-Al_2O_3	1200	23	57	Good	Good	Fair	Good	Good	Good
Beryllia-BeO	1200	5-10	393	Excellent	Excellent	Excellent	--	Fair	Good
Calcia-CaO	1000	9	170	Fair	Poor	Poor	Poor	Fair	Fair
Magnesia-MgO	1200	22	61	Fair	Poor	Good	Poor	Good	Fair
Silica-SiO_2 (crystalline)	1100	--	38	Poor to 320°C excellent above 320°C	Poor	Poor	Good	Poor	Poor
Silica (vitreous)-SiO_2	--	0- 9	33	Excellent	Fair	Good	Good	--	--
Thoria-ThO_2	1200	17	0	Poor	Good	Fair	Poor	Good	Excellent
Zirconia-ZrO_2 (stabilized)	1200	28	22	Fair	Good	Fair	Good	Poor	Good
Mullite - $3Al_2C_3 \cdot 2SiO_2$	1200	30	64	Fair	Fair	Fair	Good	Fair	Fair
Spinel - $MgO \cdot Al_2O_3$	1300	36	50	Poor	--	Fair	Fair	Good	--
Zircon-$ZrO_2 \cdot SiO_2$	1000	30	50	Good	Fair	Fair	Good	Poor	Good

Assuming that a ceramic shot sleeve would be preheated, consider Figure 10. Here, the tensile, bend, and compressive strengths of a number of single oxides are compared at 70°F and 2000°F. Based on these data and the data presented in Table II, aluminum oxide (Al_2O_3) appears most promising. It exhibits the greatest overall mechanical strength of the various oxides, both at room temperature and at 2000°F. Moreover, it is the hardest of the commercial refractory oxides and should consequently show the best abrasion resistance, all other factors being equal. Wear resistance has been shown to be related directly to hardness in metals,[7] and this should extend to ceramic oxides as well. Indeed, the excellent hardness and elevated-temperature properties of alumina have been reflected in high-speed, sliding bearing, and seal material in studies conducted at Battelle Memorial Institute.[8] In addition, the fact that Al_2O_3 (as well as many of the other refractory oxides) has much higher heat-of-formation and free energy-of-formation values than any of the iron oxides[9] explains why virtually no reaction or wetting occurs between molten iron and alumina.

In summary, aluminum oxide was selected as the outstanding prospect for a ceramic shot sleeve because of its unusual combination of attractive properties. These include:

a. Excellent hardness

b. Dimensional stability at elevated temperatures

c. Virtual inertness to molten ferrous alloys

d. Good room temperature and elevated temperature tensile strength

e. High compressive strength

f. Very high modulus of elasticity

g. Nongalling characteristics

h. Excellent wear resistance

i. Ease of fabricability.

Having selected alumina as the preferred, ceramic, shot sleeve material was only a partial solution to the materials selection problem, however. The properties of Al_2O_3 can vary widely, depending on its purity and density, as well as the fabrication method and firing parameters used. While a theoretically dense, fine-grained material has the greatest high-temperature strength and hardness, its resistance to thermal shock is not as good as that of a somewhat

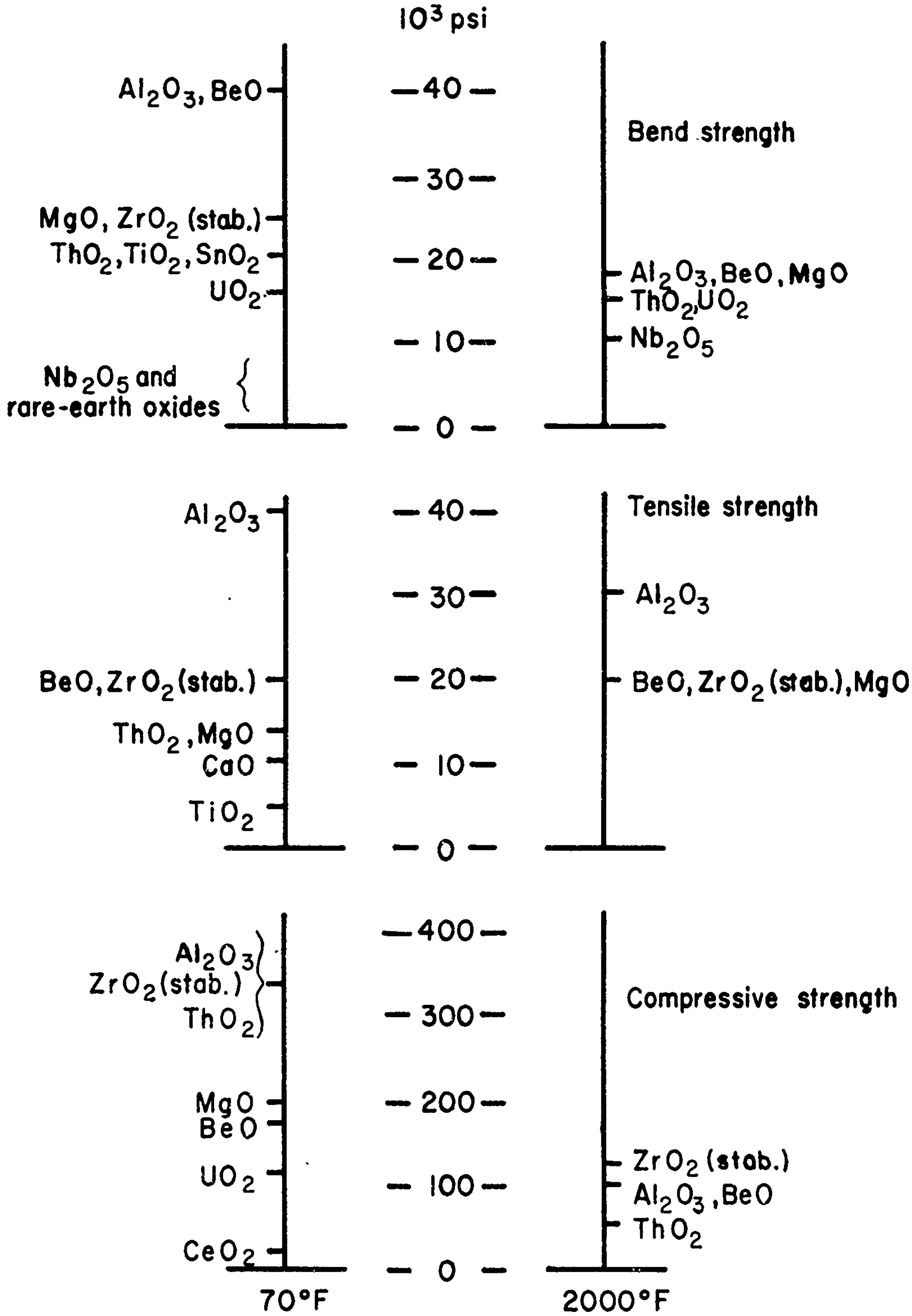

Figure 10 - tensile, bend, and compressive strengths of single oxides at 70°F and 2000°F.

porous, coarse-grained body. A small amount of porosity is helpful in cushioning the mechanical stresses which are associated with steep thermal gradients, and the coarse particles act as crack stoppers. A coarse-grained body thus appears to have the greatest potential for the present application.

For these reasons, Hycor TA-509 alumina was selected as the prime candidate for a ceramic shot sleeve and plunger.* This composition had been used successfully in several applications in contact with molten steel; and ware made from this composition exhibits excellent abrasion resistance, in spite of its moderately low strength. It may be fabricated by slip casting, using plaster molds, and the use of this technique results in ware with good resistance to thermal shock. Some of the properties of Hycor TA-509 alumina are summarized in Table III.

An alternate solution to the shot sleeve materials problem appeared to be the use of a composite material; one which would embody the best features of its several components. One family of composite materials that has been used widely for steel pouring and casting applications is the graphite-ceramic composites. These composites can vary over a wide range of compositions, but they all contain natural flake graphite and a ceramic component, such as Al_2O_3, MgO, or refractory clay and grog mixtures. Their generally good properties and easy fabricability made them appear to be good prospective shot sleeve materials. Natural graphite contributes thermal shock resistance to the composite, by increasing its thermal conductivity and reducing its coefficient of thermal expansion; and while all graphites are attacked by molten steels, this is least true for natural graphite.

Graphite-ceramic composite bodies are readily fabricated into tubular shapes using isostatic pressing techniques and can be machined to close tolerances. Sound bodies containing 15% to 20% porosity are produced by firing at 2500°F.

The graphite content of the graphite-ceramic composites makes them moderately susceptible to high temperature oxidation, and they were expected to require oxidation protection. It was felt that jacketing the composite in a metal sleeve would protect the outer surface from oxidation; whereas adequate protection for the inner surface might be achieved by introducing a reducing or an inert atmosphere, or by applying a protective coating. It was also anticipated that the graphite content of some of the composites might be sufficient to permit them to act as susceptors, in the event that induction heating of the graphite-ceramic shot sleeve proved necessary.

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*Hycor TA-509 is produced by the Engineered Ceramics Division of Sola Basic Industries.

Table III-- Properties of Hycor TA-509 Alumina

Composition	98+% Al_2O_3, .2% Clay
Density	0.116 lbs/in^3 or 3.21 g/cc
Apparent porosity	17%
Room-temperature modulus of rupture	1600 psi
Fusion temperature	3570°F
Coefficient thermal expansion	4.0×10^{-6} in/in/°F
Thermal conductivity	19 BTU/hr/ft^2/°F/in
Relative abrasion resistance	Excellent

Nominal compositions and some generalized properties for three graphite ceramic composites that were considered promising are presented in Table IV. Based on its relatively greater high-temperature strength and erosion resistance, Composite 65A, consisting of natural graphite and fused alumina, was selected as the most promising graphite-ceramic composite shot sleeve material. Some pertinent properties of Composition 65A are presented in Table V. This material has excellent thermal-shock resistance and satisfactory resistance to erosion by molten ferrous metals. The relatively low mechanical strength of the composite was not deemed to be detrimental since, in use, the sleeve would be supported externally by a high-strength metal alloy shell.

Several sleeves of Composition 65A were received from the manufacturer, and close examination revealed that the material had poor abrasion resistance. Thus, while the composite contains a very hard component, fused alumina grain, the constituents are held together by a carbonaceous bond of relatively low strength. Whereas the poor abrasion resistance of this composition does not appear to be a significant disadvantage in casting or pouring applications, it would be a severe drawback in the shot sleeve application. The plunger movement might well produce excessive wear in the bore of the shot sleeve, which would quickly result in a poor dimensional fit and cause serious difficulties.

A second family of composite materials that seemed to have potential for application as shot sleeves for ferrous die casting was the fiber-reinforced ceramics. Previous studies had shown that the thermal shock resistance of ceramics is markedly improved through reinforcement with metal fibers.[10] Propogation of cracks in the ceramic matrix is suppressed, and the fiber mesh framework maintains overall specimen integrity, despite the existence of numerous fine cracks. Al_2O_3, MgO, ThO_2, ZrO_2, mullite, and zircon have been used as matrix materials, and tantalum, tungsten, and molybdenum fibers have been employed as reinforcement metals.

Hot pressing is the only fabrication method capable of producing sound, dense, fiber-reinforced compacts, however; and at the time, only small test specimens suitable for laboratory evaluation had been fabricated. It appeared, therefore, that a development program would be needed to optimize the properties of fibre-reinforced materials and to upscale the current fabrication techniques to produce pieces as large as shot sleeves.

Table IV--Composition and Properties of Some Graphite-Ceramic Composites

Code Number*	Approximate Composition (Wt %)	Porosity (%)	Thermal Expansion (10^{-6} in/in/°C)	Qualitative Thermal Conductivity	Fabri-cability	Machin-ability
65A	65 Fused Al_2O_3 35 Natural graphite	15	4-6	2 to 3 times that of Al_2O_3	Good	Good
37A	65MgO 35 Natural graphite	15	5-7	3 to 4 times that of MgO	Good	Good
716	50 Zircon 25-35 Refractory clay** 20 Natural graphite	18-20	6-8	About 2 times that of mullite	Good	Good

*Vesuvius Crucible Company, Monroeville, Pennsylvania

**The clay has the following composition - 33% Al_2O_3 and 67% SiO_2. It has a softening temperature of about 3090°F

Table V--Some Mechanical Properties of Graphite-Ceramic Composition 65A

Property	Room Temperature	2500°F
Modulus of rupture, psi	2000	--
Ultimate compressive strength, psi*	2200	1750
Percentage compression*	2.3	4.5
Young's modulus, psi	0.74×10^6	0.33×10^6
Thermal expansion (70°F - 2200°F)	27×10^{-7} in/in/°F	

*Loading rate 500 pounds per minute

It was felt that alumina would most probably qualify as the best ceramic matrix, but starting materials and fabrication procedures would had to have been optimized. In addition, the optimum percentage and configuration of fibers would also had to have been fabricated and tested under simulated service conditions in a die casting machine.

The application of permanent protective coatings to the components of the pressure injection system used for ferrous die casting is similar, in concept, to the use of composite materials. The two general reasons for applying such a coating are to increase the wear resistance of the component to which the coating is applied, or to increase its resistance to chemical attack, or both. The broad classification, resistance to chemical attack, includes oxidation resistance, resistance to being wet by the molten cast metal, resistance to becoming alloyed with the cast metal by diffusion, and resistance to soldering or welding. Another alternative, of course, is the repeated application of temporary coatings, like refractory suspensions or acetylene black. However, such coatings present several real and potential disadvantages, the first being cost. The application of such a temporary coating may increase cycle time, and its consumption of supplies represents an added indirect material cost. Temporary coatings may introduce gas into the castings, and traces of the temporary coatings themselves may become incorporated in the castings. Being cognizant of these considerations, Dort explored the possibilities of permanent coatings assiduously.

One area of specific attention was the graphite-ceramic composites. It was hoped that the wear problem might be solved by applying a refractory, abrasion-resistant coating on the internal surface of a sleeve of Composition 65A. Several ways of doing this were explored, and a number of promising materials were considered. However, most coating materials proved to be inadequate on closer examination.

The refractory oxides ZrO_2 (stabilized) and Al_2O_3 were rated unsuitable as protective coatings because of their known, poor adherence to graphite substrates resulting from the thermal expansion mismatch. Tungsten carbide (WC), titanium carbide (TiC), and titanium diboride (TiB_2) were considered. Titanium diboride showed the most promise as a coating material for this application because of its resistance to molten iron,[11] its good oxidation resistance, and its adherence to graphite substrates. However, the technology associated with TiB_2 coatings on graphite was still in a very early stage of development. Consequently, the coatings were very costly and not completely reliable These considerations seemed to preclude the use of titanium diboride as a coating for graphite-ceramic shot sleeves.

It was concluded that with no suitable coating available, graphite-ceramic shot sleeves for high-temperature-alloy die casting were impractical.

The permanent coatings considered by Dort for metallic components of the pressure injection system of a ferrous die casting operation were selected to prevent oxidation of the substrate, to increase its surface hardness, and most importantly, to eliminate wetting, alloying, and soldering by the molten metal.

The ceramic oxides were given primary emphasis as coating materials because of their generally excellent compatibility with molten ferrous metals. Al_2O_3 and stabilized ZrO_2 were selected on the basis of extensive coating experience; the coatings were applied both by flame spraying and plasma spraying techniques. A NiAl coating, which supposedly produces a tough, adherent nickel aluminide, was evaluated, both in the original state and after a preoxidation treatment. Molybdenum disilicide coatings were applied to TZM by means of the slurry silicide technique and the diffusion metalliding technique. The technical description and sources of the coatings are shown in Table VI.

All of the coatings (except the $MoSi_2$ formed by diffusion metalliding) were applied to a 4" length of 1/2" diameter cylindrical samples. (The diffusion metallided coating of $MoSi_2$ was applied to the bore of a TZM shot sleeve and to the outer diameter and face of a TZM plunger. Both were evaluated as part of the effort directed toward characterizing the thermal environment in which the pressure injection system of a ferrous die casting operation must perform. The results of that evaluation are reported later in this section.) A fixture was constructed to test eight samples simultaneously. The test consisted of immersing the samples in molten malleable iron at 2600°F - 2700°F for 6 seconds, followed by a 54 second cooling period in air; this cycle was repeated 50 times.

Although the test cycle in the molten iron approximated the cycle used in the die casting machine, the temperatures attained by the test samples were considerably higher than the equilibrium temperature occurring in the shot sleeve during the casting operation, owing to the geometric differences and the much greater relative proportion of molten metal in the immersion test.

Higher temperatures result in greater stresses at the coating-substrate interface, due to differences in thermal expansion coefficients. Therefore, from the standpoint of thermal shock, immersion in molten iron constitutes a rigorous test, wherein coating deterioration should occur at an accelerated rate compared with that in a shot sleeve.

Table VI--Protective Coatings Evaluated for Use on the Metallic Components of the Pressure Injection System

Designation	Description	Source
LA-6	99% Al_2O_3	Union Carbide Corporation, Materials Systems Division, Coatings Service
LZ-1	Stabilized - ZrO_2	Union Carbide Corporation, Materials Systems Division, Coatings Service
MZ	Stabilized - ZrO_2	Metallizing Company of America
NC-101-A	Pack Aluminide - NiAl (as coated)	Sylvania Electric Products, Incorporated Chemical and Metallurgical Division
NC-101-A-P	Pack Aluminide - NiAl (preoxidized)	Sylvania Electric Products, Incorporated Chemical and Metallurgical Division
R-512-G	Slurry Silicide - $MoSi_2$	Sylvania Electric Products, Incorporated Chemical and Metallurgical Division
Diffusion Metalliding	$MoSi_2$ formed by electrodeposition of silicon from a molten fluoride bath with interdiffusion of molybdenum and silicon	General Electric Company Dover Wire and Fabrication Operation Lamp Metals and Components Department

It must be noted, however, that the severe mechanical shear stresses that may occur during operation of a shot sleeve would not be simulated by a static immersion test; and, in this respect, the test would not be sufficiently rigorous. Nevertheless, the ability of a coating to successfully withstand repeated thermal cycling is certainly necessary to enable it to resist the mechanical stresses occurring during operation of the sleeve.

The results of the tests are presented in Table VIII. The plasma-sprayed coatings of Al_2O_3 and ZrO_2 did not prevent deterioration of the substrate metals when conditions exceeded the capabilities of the substrates. Berylco-10 melted. TZM oxidized significantly, although the coatings of ZrO_2 provided slightly better protection than those of Al_2O_3. The plasma-sprayed coatings of Al_2O_3 and ZrO_2 did appear to reduce wetting in the case of the Rene' 41 substrate. These coatings were still partially intact after the test, although there was some spalling at the molten iron-air interface.

The flame-sprayed ZrO_2 coating successfully prevented wetting of Rene' 41 and TZM, and a significant thickness of coating remained after 50 test cycles. However, as shown in Figure 11 and Table VIII, a certain amount of attrition did occur in the coating during the test.

The X-ray diffraction data listed in Table IX revealed that the original coating, which was entirely the cubic stabilized ZrO_2 phase, had undergone some transformation to monoclinic ZrO_2 in the test and had also suffered some slag attack to form $ZrSiO_4$. Either or both of these effects, combined with the unavoidable thermal stresses, might be responsible for the partial breakdown of the coating.

The flame-sprayed ZrO_2 coating provided good oxidation resistance to the TZM substrate. There was no evidence of spalling of the coating at the molten iron-air interface.

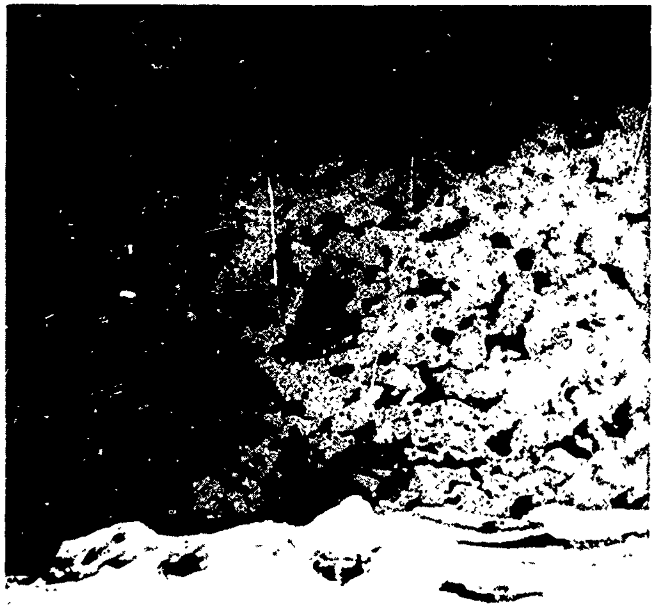
The NiAl coating did not prevent wetting of the Rene' 41 substrate or oxidation of the TZM. Preoxidation of the coating did not appear to have any noticeable effect. Results with the CRM samples were not conclusive, insofar as the effectiveness of the NiAl coating was concerned.

The slurry silicide coating did not provide any observable oxidation protection for the TZM.

Table VII--Performance of Permanent Coatings Evaluated by the Immersion Test

Substrate	Coating	Application Method	Nominal Thickness	Results
Berylco-10	Al2O3 (LA-6)	Plasma-sprayed	0.005"	Rod melted after third cycle
Berylco-10	ZrO2 (LZ-1)	Plasma-sprayed	0.005"	Rod melted after seventh cycle
CRM*	NiAl (NC-101-A)	Pack aluminide (as coated)	0.003"	Moderate wetting of substrate
CRM*	NiAl (NC-101-A-P)	Pack aluminide (preoxidized)	0.003"	Moderate wetting of substrate
Rene' 41	Al_2O_3 (LA-6)	Plasma-sprayed	0.005"	Some wetting, coating partially intact
Rene' 41	ZrO_2 (LZ-1)	Plasma-sprayed	0.005"	Some wetting, coating partially intact
Rene' 41	ZrO_2 (MZ)	Flame-sprayed	0.015"	Virtually no wetting, coating mainly intact
Rene' 41	NiAl (NC-101-A)	Pack aluminide (as coated)	0.003"	Moderate wetting
Rene' 41	NiAl (NC-101-A-P)	Pack aluminide (preoxidized)	0.003"	Moderate wetting
TZM	Al_2O_3 (LA-6)	Plasma-sprayed	0.005"	Substrate badly oxidized, coating destroyed
TZM	ZrO_2 (LZ-1)	Plasma-sprayed	0.005"	Substrate badly oxidized, coating destroyed
TZM	ZrO_2 (MZ)	Flame-sprayed	0.015"	Little oxidation, coating mainly intact
TZM	NiAl (NC-101-A)	Pack aluminide (as coated)	0.003"	Substrate badly oxidized
TZM	$MoSi_2$ (R-512-G)	Slurry silicide	0.003"	Substrate badly oxidized

*A low cost, cast super alloy that may be air melted, developed by Chrysler Corporation. Nominal composition 20% chromium, 5% nickel, 5% manganese, 2% tungsten, 2% molybdenum, 2% columbium, 1% carbon, 0.5% silicon, 0.2% nitrogen, balance iron.[12]



Mag = 200X

As-Coated

Mag = 260X

After Immersion Test

<u>Figure 11</u> - Flame-sprayed coating of ZrO_2 on René-41. Light area is metallic substrate; medium grey area is ZrO_2 coating; dark area at top is mounting plastic.

Table VIII--Attrition Observed in Flame-Sprayed Ceramic Coatings After 50 Immersion Cycles

Substrate	Coating	Coating Thickness Initial	After Immersion Test
Rene' 41	ZrO_2 (MZ)	0.017"	0.005"
Rene' 41	Al_2O_3 (LA-6)*	0.005"	0.002"
Rene' 41	ZrO_2 (LZ-1)*	0.005"	0.002"
TZM	ZrO_2 (MZ)	0.018"	0.005"

*Considerable spalling occurred at the molten iron-air interface.

Table IX--Results of X-ray Diffraction
Analysis of the Flame-Sprayed ZrO_2 Coating

As Coated			After Immersion Test		
D-Spacing (A)	Relative Intensity	Phase*	D-Spacing (A)	Relative Intensity	Phase*
2.93	Strong	c-ZrO_2	4.42	Weak	$ZrSiO_4$
2.53	Strong	c-ZrO_2	3.70	Weak	m-ZrO_2
1.80	Very Strong	c-ZrO_2	3.29	Moderate	m-ZrO_2
			3.17	Moderate	m-ZrO_2
			2.93	Strong	c-ZrO_2
			2.83	Moderately Stronger	m-ZrO_2
			2.63	Moderate	m-ZrO_2
			2.60	Weak	m-ZrO_2
			2.53	Strong	c-ZrO_2
			1.82	Weak	m-ZrO_2
			1.80	Very Strong	c-ZrO_2

Note: Data obtained with CuK-alpha radiation

*c-ZrO_2 = Cubic ZrO_2

$ZrSiO_4$ = Zircon

m-ZrO_2 = Monoclinic ZrO_2

C. Thermal Environment

For the pressure injection system of a horizontal die casting machine to operate successfully, liquid metal must be prevented from penetrating the annular clearance between the plunger and the shot sleeve or shot sleeve liner. In the pressure injection system adopted by the Lamp Metals and Components Department for ferrous die casting, this penetration was prevented by using a sliding sacrificial compression seal that compensated for distortions and dimensional variations. In aluminum and brass die casting practice, this penetration is conventionally prevented by using a system of great precision, in which very small clearances are maintained between the plunger and the shot sleeve (or shot sleeve liner) throughout the entire stroke of the plunger. To determine if the approach used for aluminum and brass might be applicable to ferrous die casting, it was necessary to determine, quite accurately, the thermal environment created in the pressure injection system of a horizontal die casting machine handling molten ferrous metals.

The Dort Metallurgical Company, which was charged with the responsibility for evaluating materials and designs for the pressure injection system of a horizontal ferrous die casting machine, decided to generate real, as opposed to calculated, data. To couple this testing program with the production of die castings, however, would have been unwise. First, the construction of even the most rudimentary die would have added unnecessarily to the cost of the testing program. The complications and restrictions of die design (e.g., on injection velocity and pressure) would have been an unnecessary burden on the testing program; and the inevitable difficulties encountered in die casting (e.g., soldering, ejection problems, and die failure) would have significantly increased the average overall cycle time. The suggested course of action, therefore, was to blank off the die end of the shot sleeve and cast biscuits only. Such an approach would use a die casting machine only as a test bed, unnecessarily making an expensive piece of equipment unavailable for more profitable enterprise. These considerations prompted the construction of a simulated die casting machine (SDCM) to serve as a test bed.

In concept, the SDCM was intended to supply a relatively inexpensive means of testing metal injection systems at high cycle rates. It was to have the following features:

a. It was to be amenable to the expeditious exchange of shot sleeves of various materials, wall thicknesses, and volume capacities

b. It was to provide for temperature measuring devices

c. It was to have a variable operating pressure capability

d. It was to have a variable plunger velocity capability, independent of the restrictions of runner systems, gating, and the necessity of producing sound castings

e. It was to be designed for simplicity of operation and maintenance.

From this conceptual design, the Lester Engineering Company, Cleveland, Ohio, prepared detailed designs and manufactured the pneumatically operated SDCM used by Dort to characterize the thermal environment in the pressure injection system of a horizontal ferrous die casting machine. (The construction of the SDCM is illustrated by line drawing in Figure 12.)

For the purpose of determining the temperature distribution in a shot sleeve used for the pressure injection of a high-temperature alloy, Dort first considered a low-carbon steel shot sleeve made from C-1020. Eighteen thermocouples were placed in the shot sleeve, and one was placed in the plug at the die end of the shot sleeve. All thermocouples were connected to two Leeds and Northrup twin recorders. The thermocouples were screwed into their prearranged positions in the shot sleeve 1/4" away from the inner working face. The relative position of the thermocouples in the shot sleeve is shown by Figure 13.

A Berylco-10 beryllium-copper plunger was used in the C-1020 shot sleeve. The plunger tip was water cooled with a cascade cooling junction. The diameter of the plunger was made only 0.002" smaller than the shot sleeve bore.

Scrap malleable iron was melted in an Inductotherm, 100KW induction melting furnace and brought up to a pouring temperature of 2600°F. A small refractory-lined ladle was preheated to ready it to transfer the molten metal from the furnace to the shot sleeve.

The shot sleeve was heated internally with a gas heater. This mode of heating was found to be inadequate, and it was not possible to reach the desired preheat temperature (600°F).

The bore of the shot sleeve and the plunger tip was protected with a coating of Dycote #40, a colloidal graphite in mineral oil, on which was superimposed a coating of XS-1040-B, a mixture of graphite and magnesium silicate.*

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*Both are proprietary products of Foseco, Incorporated, Cleveland, Ohio. XS-1040B is now known as EP-3463.

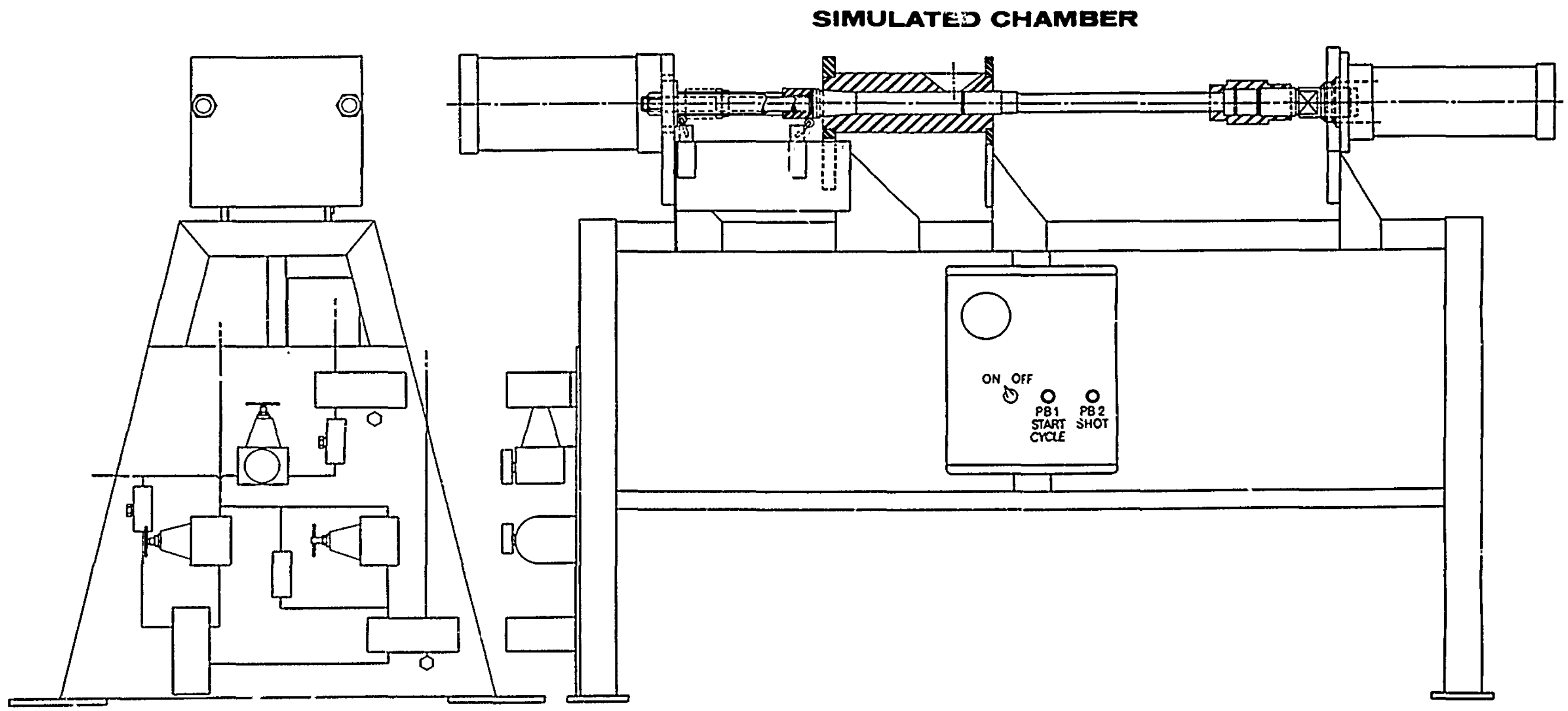


Figure 12 - Line drawing of the simulated die casting machine.

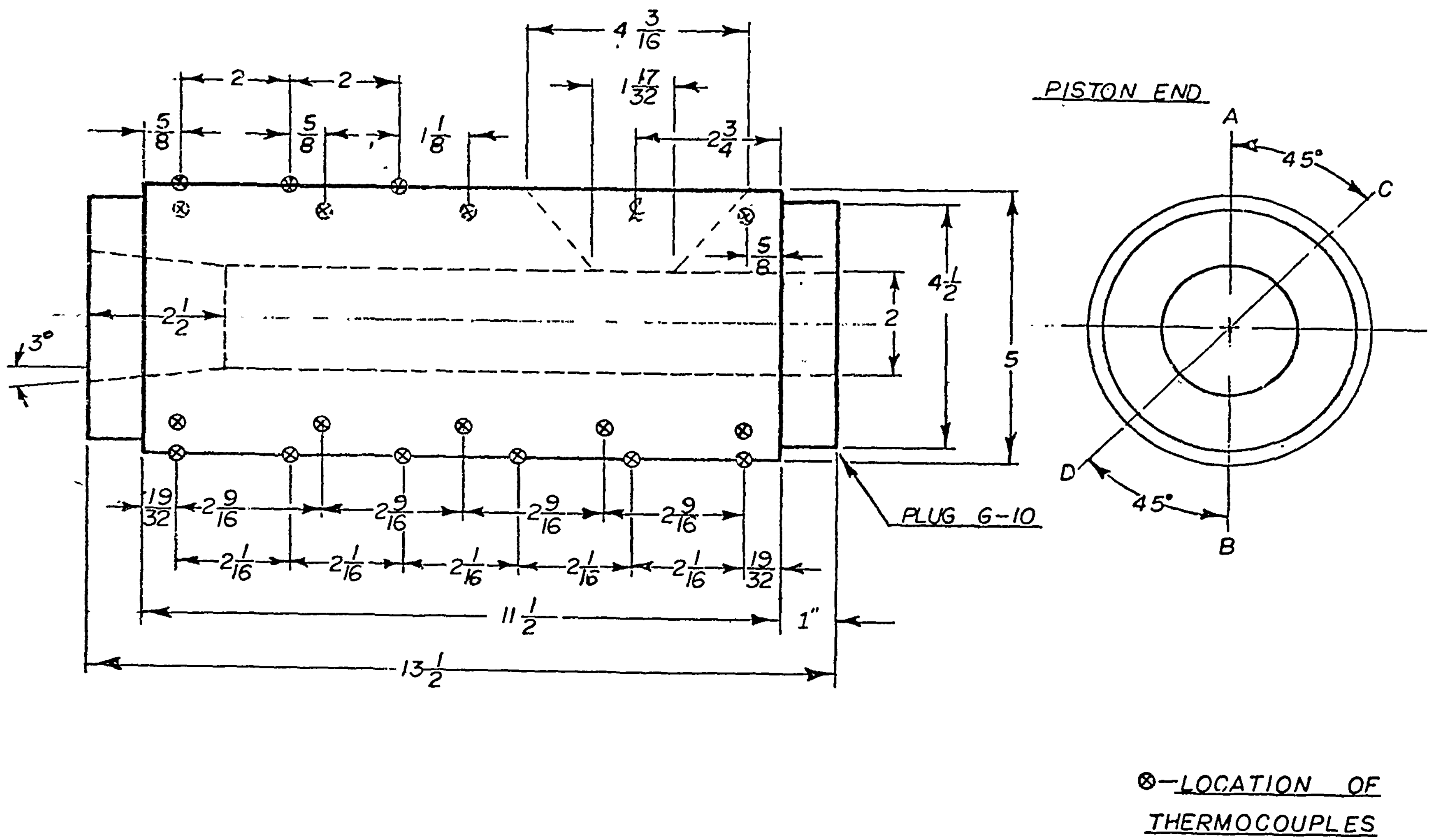


Figure 13 - Thermocouple locations in the experimental shot sleeves tested on the SDCM.

The first shot of liquid metal was allowed to remain in the shot sleeve for one minute before an attempt was made to eject it. This was done for two reasons:

a. To permit the temperature distribution at the die end of the shot sleeve to be normalized

b. To permit the biscuit to solidify completely, so that there would be no chance of rupturing a partially-solidified biscuit, i.e., experiencing an "explosion."

Figure 14 is a reproduction of the recorder chart illustrating the manner in which the temperature readings were recorded and displayed. The cyclic temperature excursions should be noted.

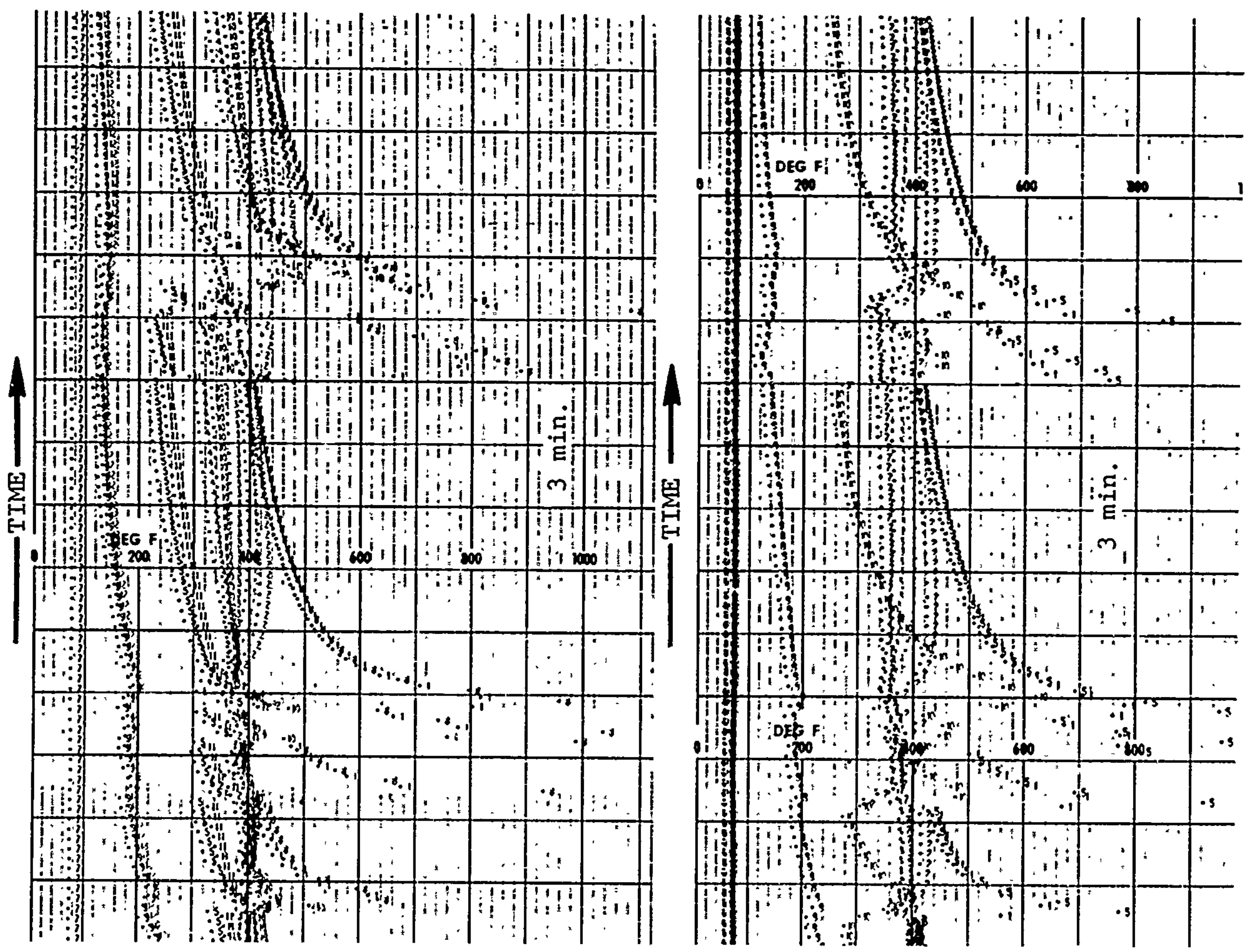
The SDCM ejected the solid biscuit quite successfully. Therefore, the dwell time was reduced from one minute to forty seconds for the second shot, and thirty seconds for the third shot. By the sixth shot, the dwell time had been reduced to five seconds without encountering ejection difficulties. On the seventh shot, however, the plunger seized in the sleeve. At that point, the supply air pressure was increased from 80 psig to 100 psig. After the ninth shot, it became obvious that greater clearance was required between the shot sleeve and the plunger. At that point, it was also found necessary to apply more of the graphite suspension to the plunger tip to prevent the liquid metal from sticking to it.

One of the objectives of the test had been to cast five pounds of metal per shot. This, however, proved extremely difficult to realize, and the shot weight varied considerably. Consequently, the temperature distribution in the shot sleeve varied considerably from cycle to cycle.

Another 15 shots were made, with varying success, in an effort to obtain biscuits having a uniform weight. Comments from the reporter's notebook are enlightening: "Extreme difficulty was encountered in extracting the biscuit due to two problems:

a. The piston (plunger) locked up in the cylinder due to insufficient clearance. It had a 0.002" clearance and it is proposed that this be opened up to 0.004" or 0.006" clearance.

b. The plunger became locked in the tapered cavity as it went forward to pressure the liquid metal into a slug ready to be ejected. This happened because the quantity of metal poured was smaller than expected.



Temperature (°F) Temperature (°F)

Upper Cylinder Temperatures Upper Cylinder Temperatures

Figure 14 - Recorder chart indication of the temperature distribution in a C-1020 steel shot sleeve.

"Difficulty was also encountered with metal freezing in the ladles each time that a shot is made. Keeping alternate ladies heated during the operation will minimize this problem until the automatic pouring device can be installed.

"Difficulty was also encountered in getting the correct amount of metal for each shot. The present method is definitely not satisfactory and should be changed."

At that point, sufficient data were in hand, and the temperature distribution measurements on the C-1020 shot sleeve were terminated.

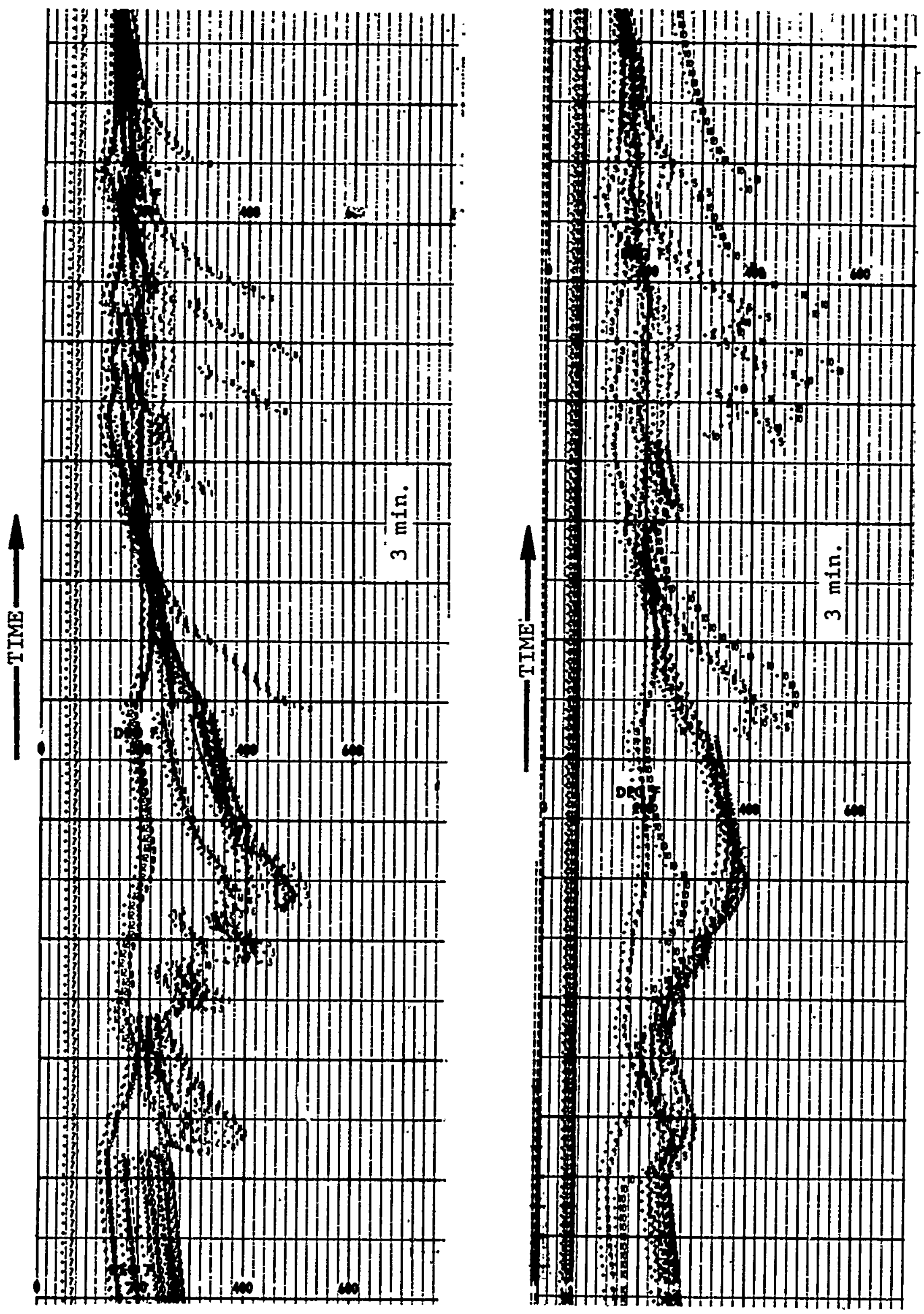
A second series of temperature distribution measurements was then made on TZM, which is known as Climelt (R) Mo by the vendor, Climax Molybdenum Company of Michigan. The material had the following composition: 0.52% titanium, 0.09% zirconium, balance molybdenum. As purchased, it had been subjected to a 1-1/2 hour stress-relief anneal at 2400°F. It was then machined into its final configuration by Dort Metallurgical Company. To give the bore of this TZM shot sleeve more oxidation resistance, a higher hardness, and hopefully, more abrasion resistance, it was diffusion metallided with silicon, yielding a protective molybdenum disilicide coating.

The test setup for the TZM shot sleeve was identical to that employed for the C-1020, except that a clearance of 0.005" was provided for the Berylco-10 plunger.

The TZM shot sleeve, too, was preheated internally with a gas heater. A special effort was made to equalize the temperature throughout the sleeve. Because of the high thermal conductivity of TZM, that proved to be a far easier task than it had been in the case of the C-1020 shot sleeve. After preheating, the temperature readings taken from the TZM shot sleeve all fell in the range of 350°F to 360°F.

As with the C-1020, the bore of the TZM shot sleeve and the tip of the Berylco-10 plunger were coated with Dycote #40. The melt of scrap malleable iron was maintained at 2600°F.

Four shots were made without water cooling the beryllium-copper plunger. Scoring was noted on the plunger; and, in spite of the $MoSi_2$ coating, scoring was also experienced in the bore of the TZM shot sleeve. Assuming the problem to be related to the thermal expansion of the plunger, water cooling was introduced. Nevertheless, the scoring became progressively worse, perhaps aggravated by an accumulation of fine debris in the annulus between the plunger and the sleeve. The test was terminated after a total of ten shots had been made. Fortunately, however, sufficient data had been collected to perform the desired analyses. Figure 15 reproduces some of the raw data as they were recorded and displayed.



Temperature (°F)
Lower Cylinder Temperature

Temperature (°F)
Lower Cylinder Temperature

Figure 15 - Recorder chart indication of the temperature distribution in a TZM shot sleeve.

The assumed failure mechanism of the TZM shot sleeve will be examined in greater detail later.

The first step in analyzing the temperature distribution data was to eliminate the confusion created by the variations in shot weight. This was achieved by taking the numerical average of the maximum temperatures recorded for each thermocouple for each shot sleeve.

$$\left(\text{i.e., } \bar{T}_{max} = \frac{\sum_{i=1}^{n} (T_{max})_i}{n} \quad \text{where n = number of shots}\right)$$

The results are presented pictorially in Figures 16, 17, 18, and 19. When considering these data, it is important to be aware that the maximum temperatures recorded were not experienced simultaneously. This is intuitively obvious when one considers the shot of molten metal being swept down to sleeve by the plunger, but it may also be clearly observed on the recorder charts. (See Figure 14.)

It must also be pointed out that the simulation of a real shot sleeve by the SDCM was imperfect at the die end of the shot sleeve. Whereas, in a real die casting operation, 50% of the shot would be delivered tc a die cavity, retaining only 50% in the biscuit and runners, the SDCM retained 100% of the shot in the biscuit. This means that if the shot were correctly proportioned for the sleeve (i e., filled the sleeve to a depth approximately equal to two-thirds of the sleeve bore with the plunger retracted), the biscuit formed by the SDCM would be disproportionately large. The affect of this was not only to introduce a disproportionately large quantity of sensible heat into the die end of the SDCM shot sleeve, but to expose a greater-than-normal length of the SDCM shot sleeve to the sensible heat of the biscuit.

Within the frame of reference outlined above, Figure 20 summarizes the temperature distribution data for the C-1020 shot sleeve by plotting the average maximum temperatures on a development of the shot sleeve. Dividing the sleeve into four zones of equal length, as indicated in Figure 20, we observe that Zone A at the plunger end of the shot sleeve is the coolest zone, whereas Zone D at the die end of the shot sleeve is hottest. There is not, however, a monotonic increase in the maximum average temperature observed as the length of the shot sleeve is traversed from Zone A to Zone D. Zone B, the zone containing the metal entry port, was found to reach higher temperatures than Zone C. It is also interesting to note that the angular or

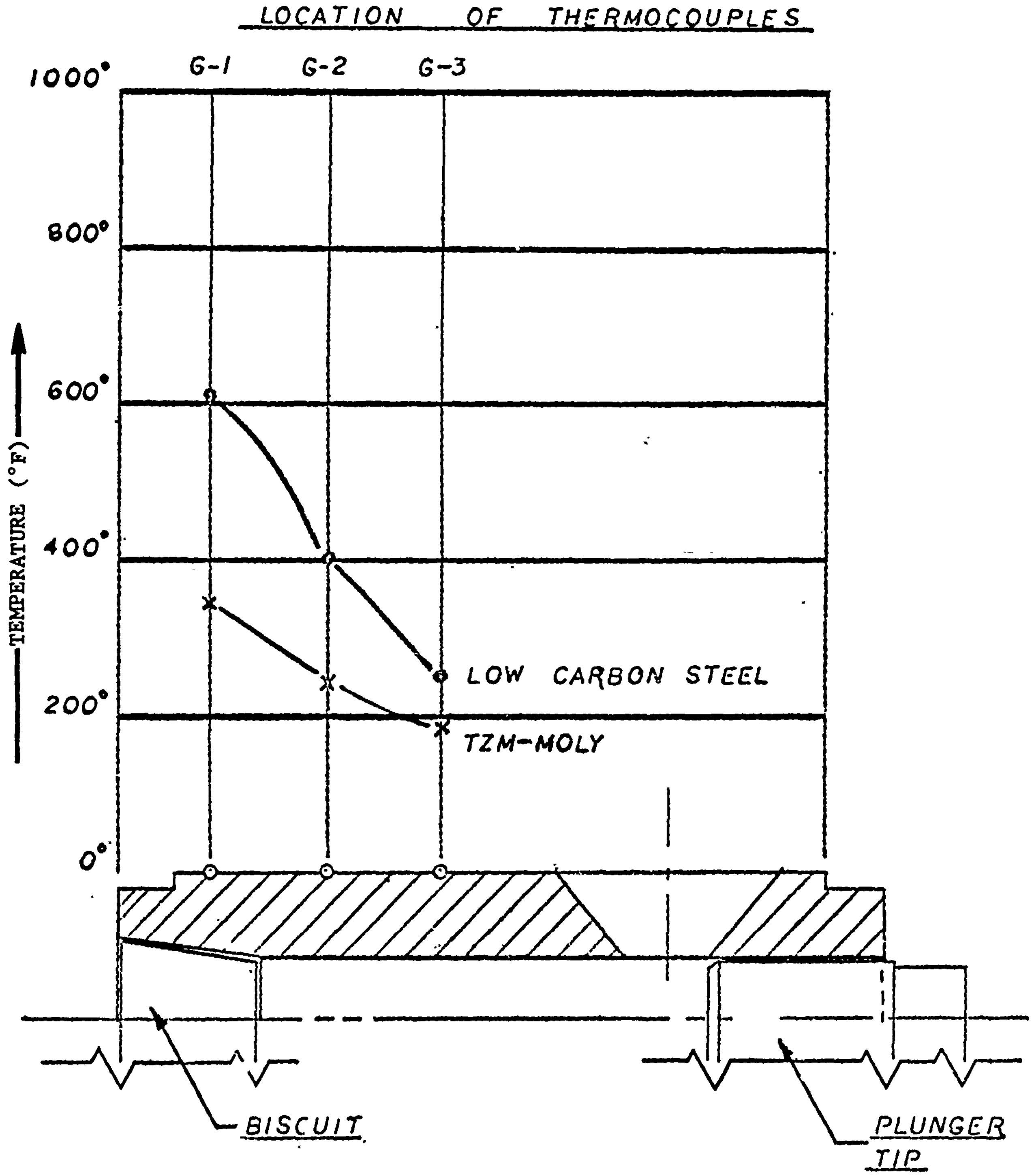


Figure 16 - Average maximum temperatures at the top of a C-1020 steel shot sleeve and a TZM shot sleeve.

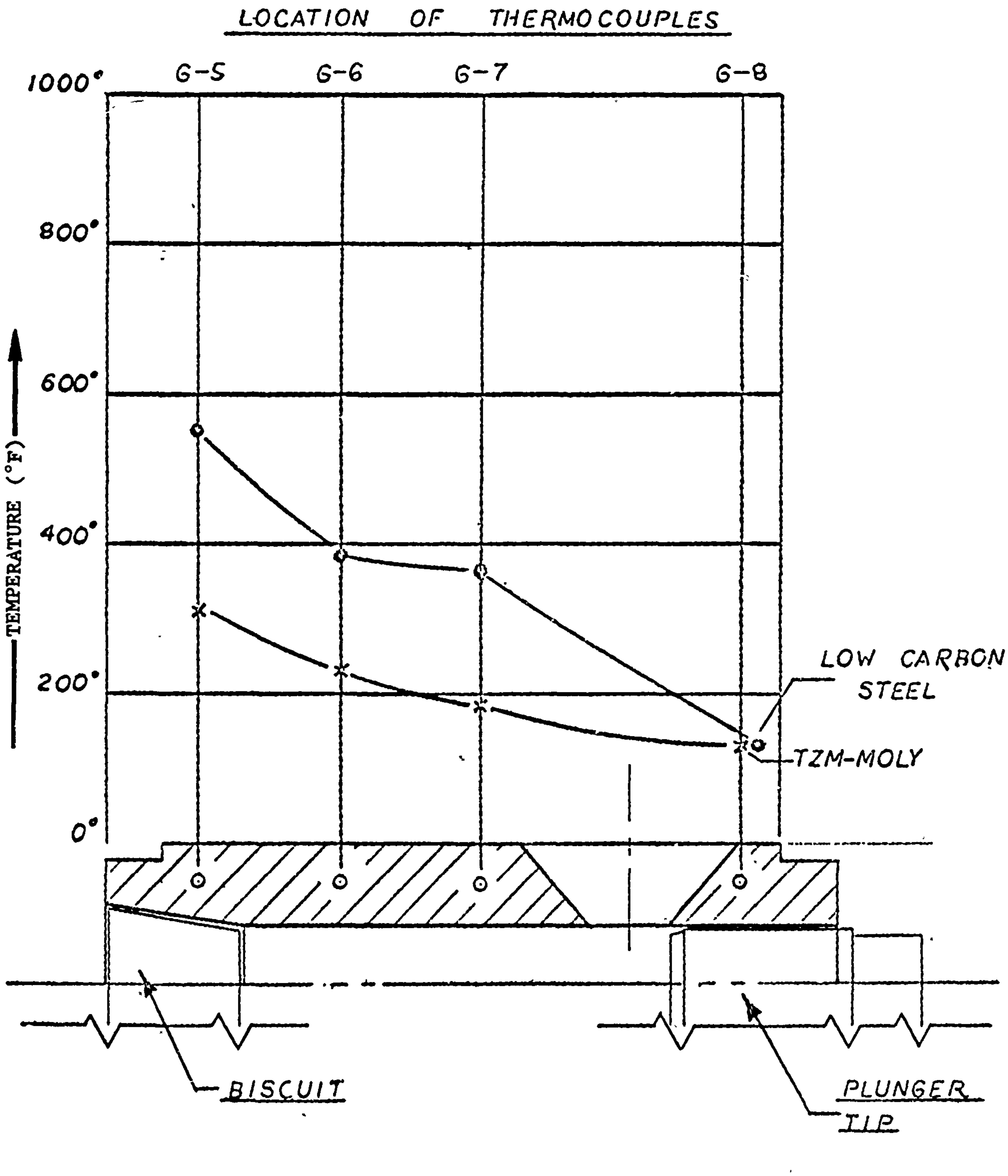


Figure 17 - Average maximum temperatures 45° from the top of a C-1020 steel shot sleeve and a TZM shot sleeve

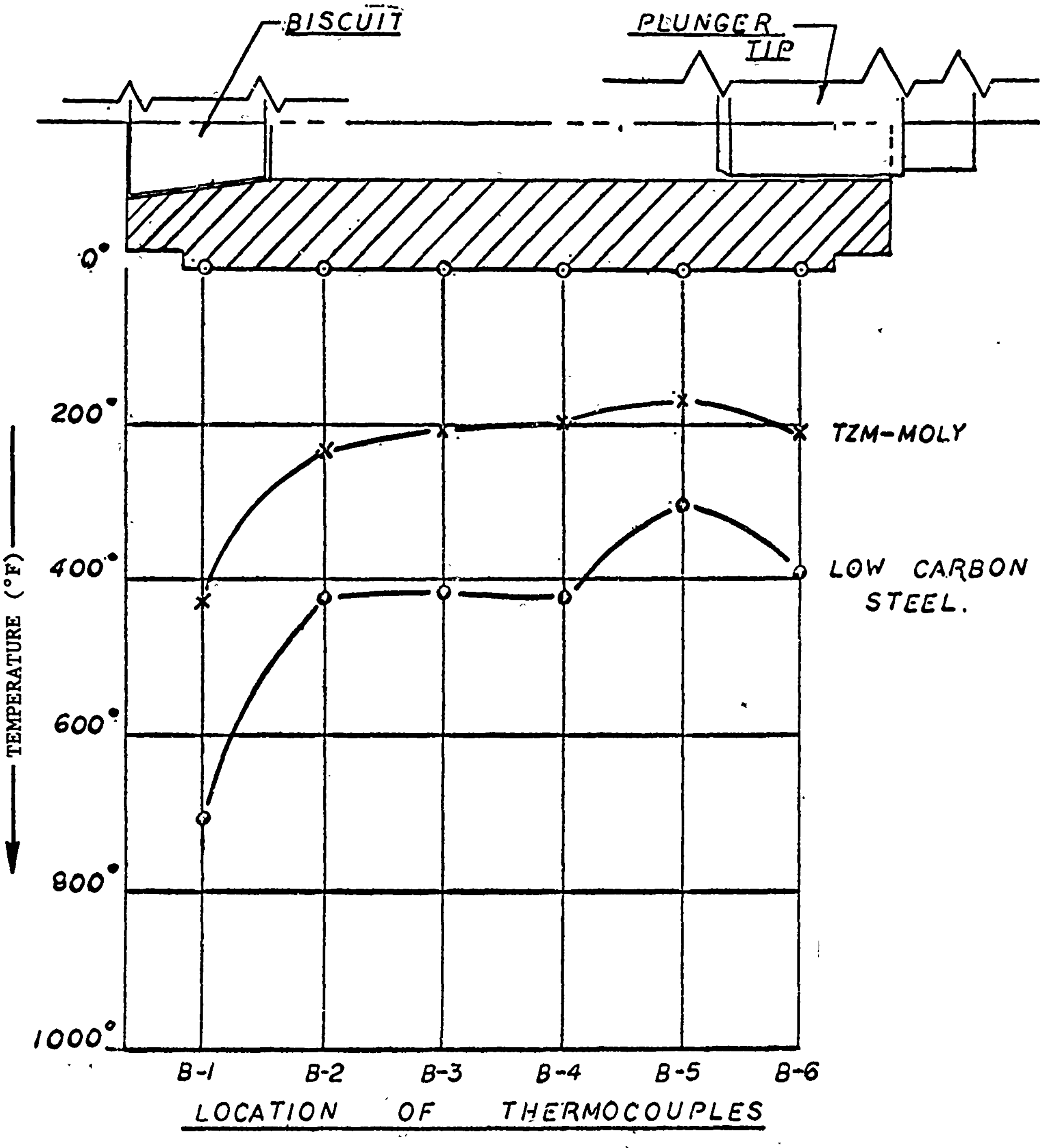


Figure 18 - Average maximum temperatures at the bottom of a C-1020 steel shot sleeve and a TZM shot sleeve.

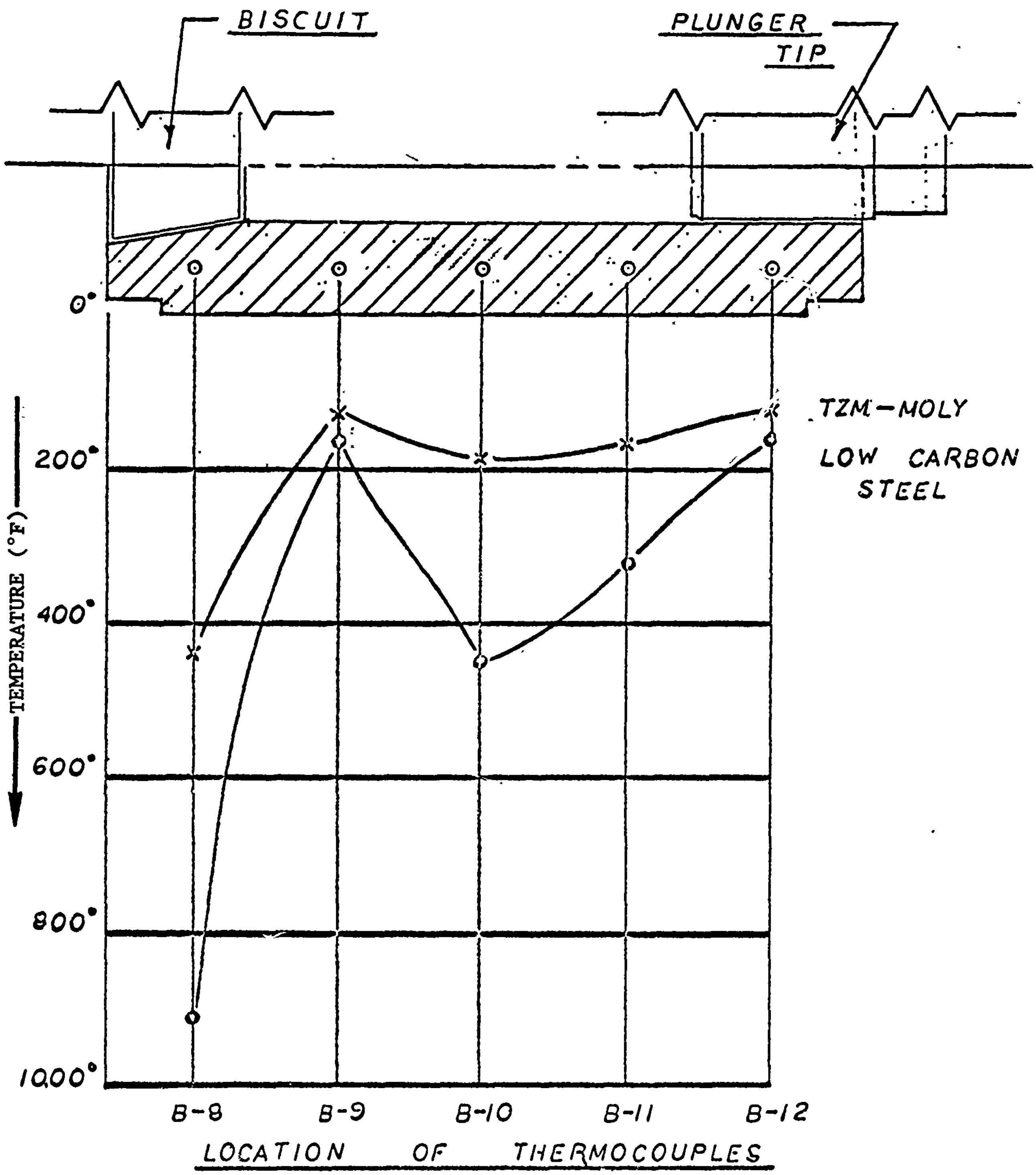


Figure 19 - Average maximum temperature 45° from the bottom of a C-1020 shot sleeve and a TZM shot sleeve.

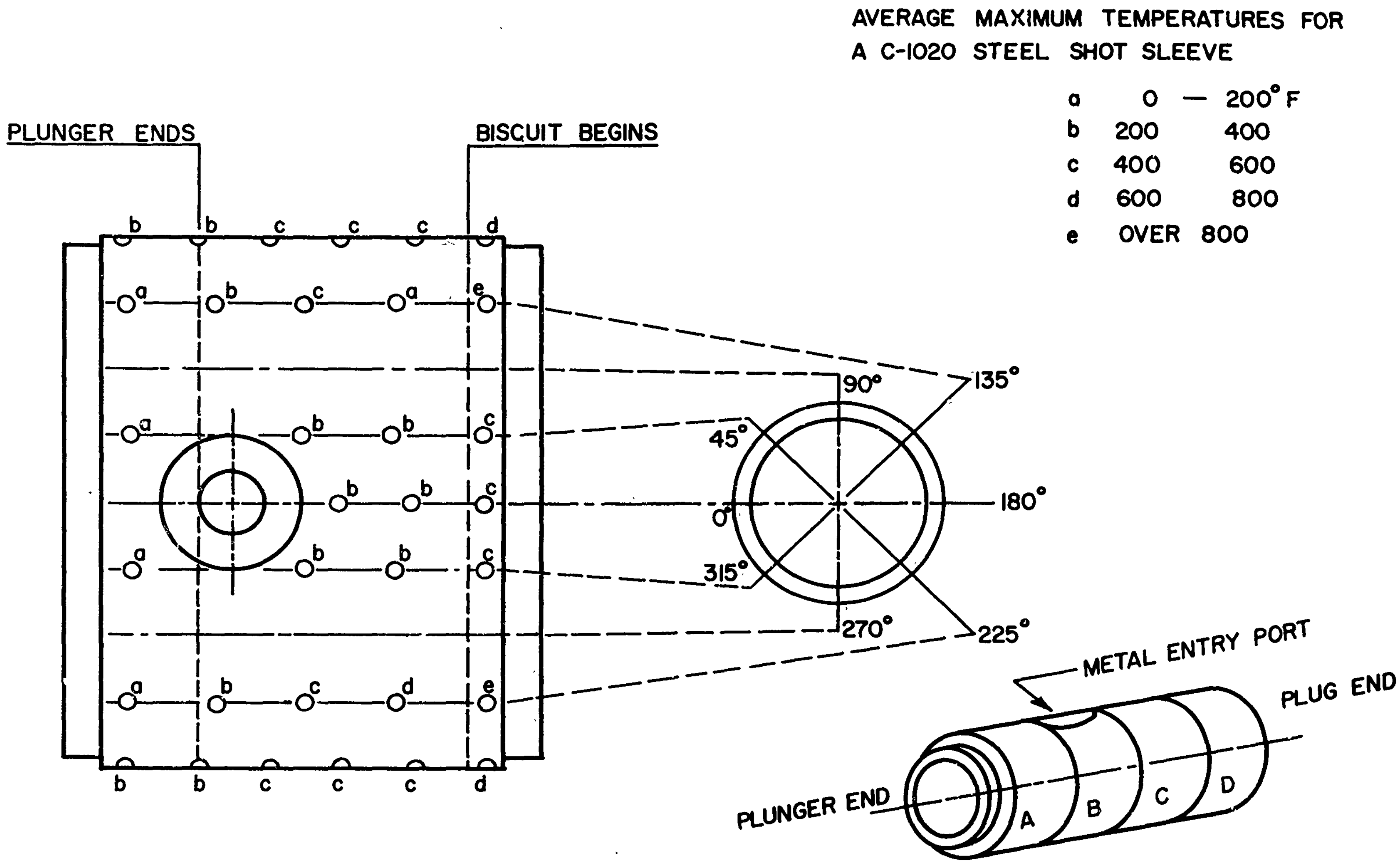


Figure 20 - Pictorial summary of the temperature distribution in a C-1020 shot sleeve.

circumferential distribution of temperatures within a given zone is not consistent from zone to zone (i.e., if the length of the shot sleeve were divided into an infinite number of rings having an infinitesimal length, and if the temperature distribution of each of these rings were established, the locus of the points of maximum temperature would not be a straight line, nor would the locus of points of minimum temperature be a straight line).

Although the maximum temperatures involved were lower for the TZM shot sleeve, the distribution was quite similar to that observed in the C-1020 shot sleeve.

The nature of the die casting process introduces another complexity in the thermal environment of the pressure injection system that was not detected by the instrumentation used in this experiment but is intuitively obvious. Just as the metal is delivered as discreet shots, the heat flux is delivered in pulses which must be dissipated by radial heat flow through the wall of the shot sleeve. The result is that the inner surface of the shot sleeve may be at a very high temperature, while the outer surface remains at its mean temperature; and, rather than experiencing a general increase in diameter, the shot sleeve may retain its mean outer diameter while experiencing an increase in its wall thickness and a decrease in its inner diameter.

Because it was a moving part, and because it was water cooled, the plunger proved difficult to instrument with a thermocouple. To circumvent this difficulty, the temperature of the plug at the die end of the SDCM was monitored. The temperature readings recorded for the plug were assumed to be equally representative of the plunger. The temperature recorded ranged from 100°F to 600°F.

The cumulative result of these temperature variations is a corresponding variation in the clearance between the plunger and the shot sleeve that is a function of both time and position. That variation, however, is reversible and is faithfully reproduced during each casting cycle.

Consider the changes which might occur in a system composed of a 2" diameter beryllium-copper plunger and a C-1020 shot sleeve having a 2" diameter bore and a 1-1/2" wall. The coefficients of expansion of these materials are assumed to be 9.3×10^{-6} and 6.5×10^{-6} in/in/°F, respectively. Assume that the system is preheated to 300°F, and that a 1/4" thick, incremental ring on the inner surface of the shot sleeve reaches 1100°F, while the bulk of the sleeve remains at 300°F.

a. The expansion of the plunger will be:

$528°F \times 2'' \times 9\ 3 \times 10^{-6}(°F)^{-1} = 9800 \times 10^{-6}''$

b. The expansion of the sleeve will be:

$228°F \times 2'' \times 6.5 \times 10^{-6}(°F)^{-1} = 2960 \times 10^{-6}''$

c. The total increase in the wall thickness of the sleeve will be:

$800°F \times 1/2'' \times 6.5 \times 10^{-6}(°F)^{-1} = 2600 \times 10^{-6}''$

d. The change in clearance will be:

$[2960°F-9800°F-2600°F] \times 10^{-6} = -9440 \times 10^{-6}''$ or -9.4 mils

Now, consider replacing the C-1020 shot sleeve with a TZM shot leeve, preheated to 350°F. Assume that the coefficient of expansion f TZM between 70°F and 550°F is 3.03×10^{-6} in/in/°F. Because the hermal conductivity of molybdenum-base alloys is very high (see igure 9), the maximum temperatures measured on the SDCM were low. or the purpose of this calculation, assume that a 1/4" thick, incre- ental ring on the inner surface of the shot sleeve reaches 650°F, hile the bulk of the TZM sleeve remains at 350°F The beryllium- opper plunger will again be assumed to reach 600°F.

a. As before, the expansion of the plunger will be:

$528°F \times 2'' \times 9.3 \times 10^{-6}(°F)^{-1} = 9800 \times 10^{-6}''$

b. The expansion of the TZM sleeve will be:

$278°F \times 2'' \times 3.03 \times 10^{-6}(°F)^{-1} = 1680 \times 10^{-6}''$

c. The total increase in the wall thickness of the TZM sleeve will be:

$300°F \times 1/2'' \times 3.03 \times 10^{-6}(°F)^{-1} = 455 \times 10^{-6}''$

d. The change in clearance will be:

$[1680°F-9900°F-455°F] \times 10^{-6} = -8575 \times 10^{-6}''$ or -8.6 mils

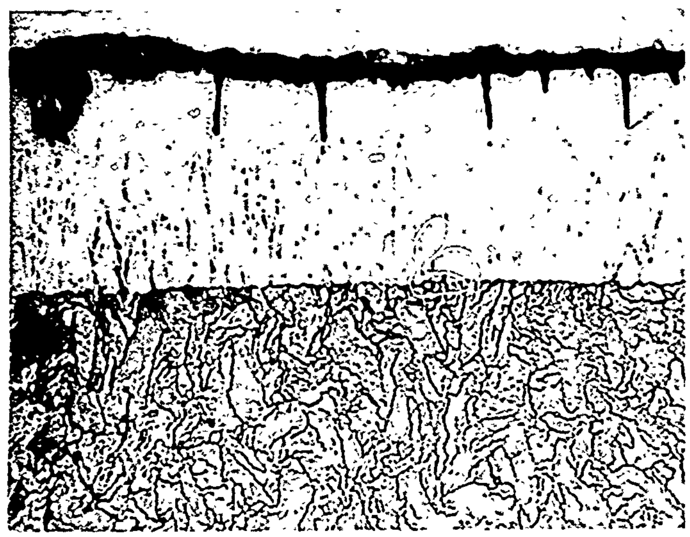
It is quite obvious that the beryllium-copper makes the greatest contribution to the reversible change in clearance between the plunger and the bore of the shot sleeve. If, by water cooling, the plunger were maintained at 200°F, the change in clearance for the systems incorporating the C-1020 steel shot sleeve and the TZM shot sleeve would be -2020×10^{-6}" (or -2.0 mils) and -1155×10^{-6}" (or -1.2 mils), respectively. A further improvement could be realized by employing a low expansion material as the plunger (e.g., a molybdenum-base alloy or a magnetrostrictive, nickel-iron or nickel-cobalt-iron alloy).

Returning to the failure of the TZM shot sleeve, it is obvious from the above calculation that operating it with an uncooled, beryllium-copper plunger was a mistake. Had the plunger been water cooled initially, the 5 mil clearance provided should have been adequate. (This may be substantiated by considering the C-1020 steel shot sleeve which operated well with a water cooled plunger after the original 2 mil clearance had been opened to 6 mils.)

The scoring encountered in the TZM shot sleeve raised the question of surface hardness of that alloy. The hardness of hot-rolled C-1020 is typically 140 BHN.[13] Recrystallized TZM, on the other hand, typically yields a microhardness value of 250 DPH, which can be converted approximately to 235 BHN. In addition, the TZM shot sleeve was silicided. Such a treatment normally yields a 2 to 3 mil layer of $MoSi_2$ typically having a surface hardness of 1200 DPH. Assuming that the siliciding treatment was successful, the failure of the TZM sleeve can hardly be assigned to a lack of hardness.

Another explanation for the scoring noted on the beryllium-copper plunger and in the TZM shot sleeve in which it was used was that the $MoSi_2$ deposited on the TZM might have fractured and spalled, filling the annulus between the plunger and the shot sleeve with an extremely hard abrasive. To test the credibility of that hypothesis, the Lamp Metals and Components Department obtained from Dort one of the TZM plungers that had been silicided at the same time the TZM shot sleeve was silicided. That plunger was sectioned parallel to its face and was examined metallographically. The $MoSi_2$ coating was found to be discontinuous; and, in the area where there was coverage, the coating was observed to be cracked. That the apparent discontinuities are real, and not just a function of metallographic preparation, is demonstrated by the fact that the level of the TZM in the area of the discontinuity is higher than it is in the areas where an interdiffusion of molybdenum and silicon has occurred. These features are illustrated by Figure 21. Figure 22 illustrates a $MoSi_2$ coating more typical of the process capabilities.

Figure 21 - TZM plunger, diffusion silicided electrolytically in a molten fluoride bath. Note the cracks and discontinuity in the $MoSi_2$ coating.



Murakamis Etch   Mag. = 500X

<u>Figure 22</u> - Wrought, powder metallurgy molybdenum, diffusion silicided electrolytically in a molten fluoride bath.

Although the TZM shot sleeve was not sectioned, the integrity of the $MoSi_2$ coating achieved on the inner diameter of that sleeve must be viewed with skepticism, based on the evidence of the plunger.

It has been noted earlier that the angular temperature distribution is not uniform in the several zones along the length of the shot sleeve. (See Figure 20.) This suggested that the plungers might be experiencing nonuniform wear, as the result of the nonuniform expansion of the shot sleeve. Acting on this hypothesis, the used plunger was inspected at several locations with a Micrometrical Proficorder equipped with a Rotary Pilotor attachment.* No significant dimensional changes were observed.

Because the dimensional change that the plunger undergoes during each die casting cycle is of such importance in determining the clearance between the plunger and the shot sleeve, and because the variability in the plug temperatures measured on the SDCM was so great, Dort felt that a more sensitive measurement of plunger temperature was necessary. To accomplish that end, Dort turned again to simulation. The plunger and plunger rod were assembled and held in a horizontal position by large blocks of marinite, a high-temperature insulating board produced from asbestos and diatomaceous silica by Johns-Manville. In addition to supporting the assembly, the marinite restricted radial heat loss from the plunger. Holes were drilled through the marinite perpendicular to the axis of the plunger to provide access for thermocouples. The plunger and plunger rod were water cooled. Large blocks of high-density, pressed and sintered molybdenum heated to 2900°F served as a heat source, simulating the effect of the molten shot in the die casting process. Figure 23 is a diagrammatic representation of the test setup.

The placement of the thermocouples on the plunger is illustrated by Figure 24. The plunger was of standard design for use on the Lester 400 ton die casting machine.

In the first, exploratory test, a Berylco-10 plunger was used. The block of molybdenum, heated to 2900°F, was moved into contact with the face of the plunger and held for an extended period of time, in order to develop a curve indicating the rate of heat transfer from the molybdenum plate to the water-cooled plunger. The result is illustrated by Figure 25. The temperature recorded on Thermocouple 1, 1/8" away from the face of the plunger, reached a maximum temperature of 970°F, whereas Thermocouple 4, 1" from the face of the plunger, recorded a maximum temperature of only 160°F. Thermocouples more remote from the face of the plunger showed no response whatsoever to this test.

*Proficorders are a product of the Industrial Metrology Division of the Bendix Corporation, Ann Arbor, Michigan.

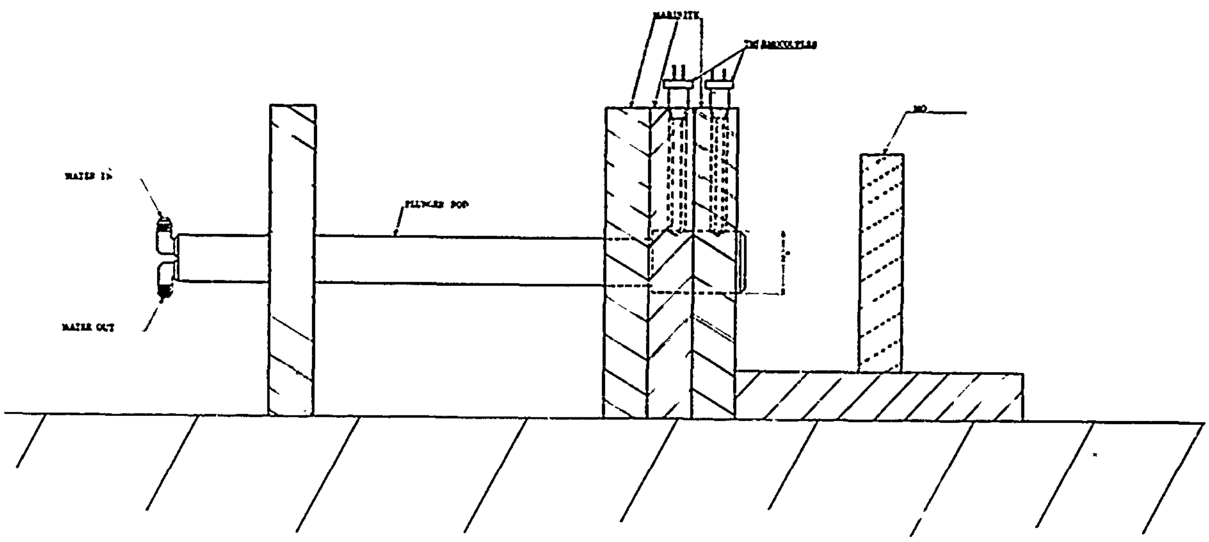


Figure 23: Drawing of the test setup devised to simulate the thermal response of plungers to the die casting process.

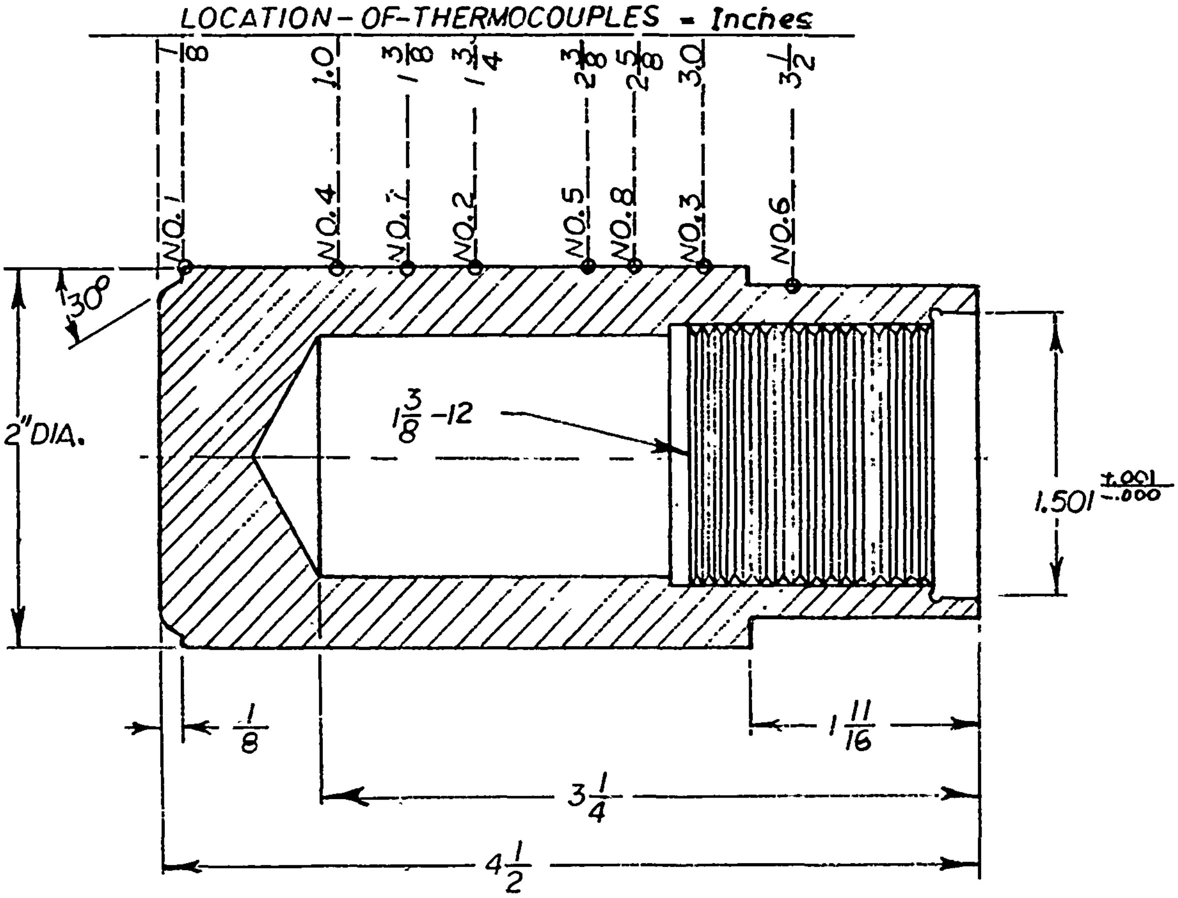


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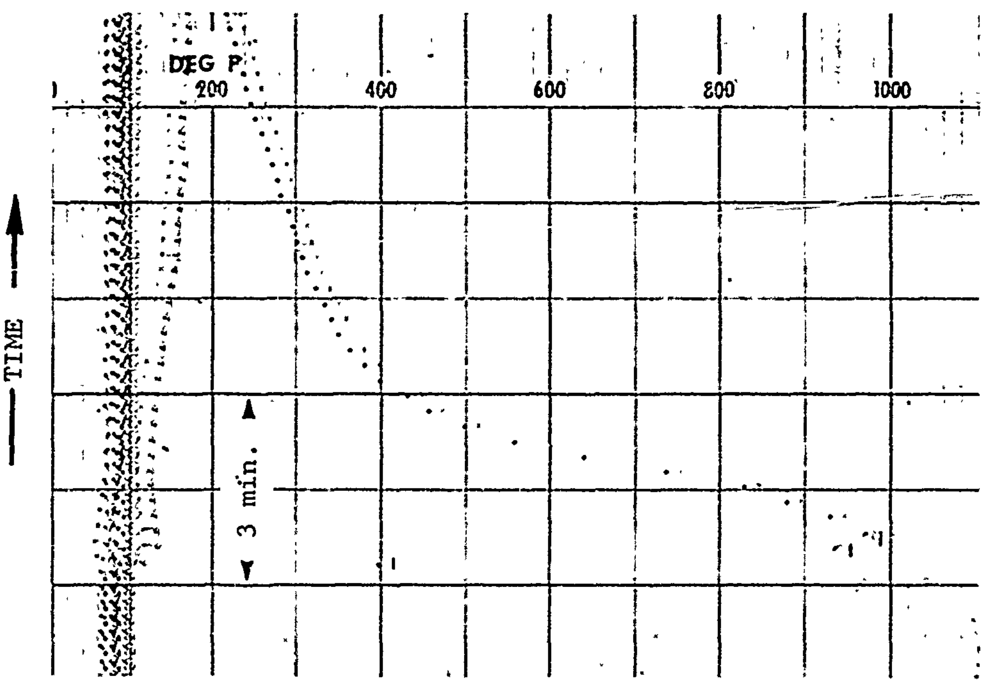
Figure 24: Sectional view of the plunger used to simulate the thermal response to die casting.



Temperature (°F)

Figure 25 - Recorder chart indication of the thermal response of a beryllium-copper plunger to prolonged contact with a molybdenum block heated to 2900°F.

The dwell time, which is the lapsed time between the completion of the plunger stroke and the opening of the die, is determined by two boundary conditions:

a. The desire to eject the part before it shrinks onto cores and/or damages the die

b. The necessity of providing sufficient time for the biscuit to completely solidify, in order to avoid biscuit bursting or "explosions."

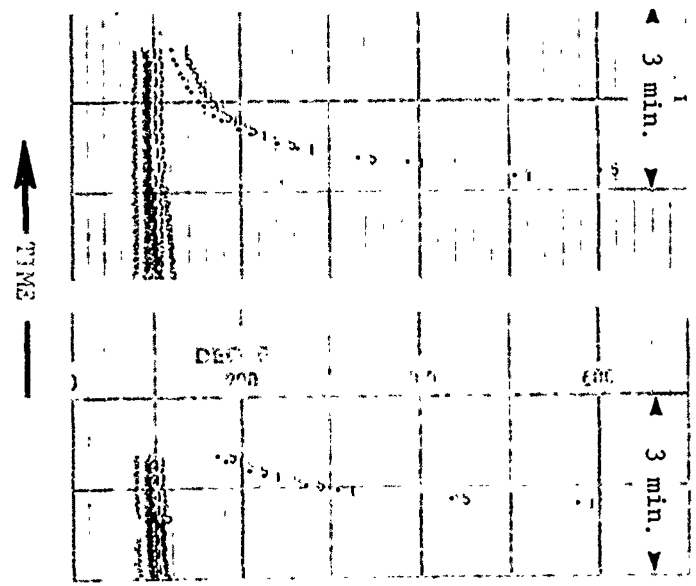
Slightly different dwell times, therefore, may be appropriate for different materials. The experience gained by Dort in a cooperative program with the Research and Development Center had indicated that dwell times of six to nine seconds were appropriate for the ferrous alloys considered.*

Using these figures, the thermal response of Berylco-10, TZM, and Invar-36 (0.12% carbon, 0.90% manganese, 0.35% silicon, 36.00% nickel, 0.20% selenium, balance iron) to contact with molybdenum blocks heated to 2900°F was determined for contact times of six and nine seconds, simulating dwell times of the same duration. To obtain a significant number of temperature indications in these short contact times, Channels 1, 5, and 9 were all connected to a thermocouple located 1/8" from the face of each plunger. This was justified by the negligible response indicated by the more remote thermocouples in the first exploratory test. The test results are summarized in Figure 26. In every case, maximum temperatures of 600°F to 700°F were recorded.

D. Evaluation of Systems

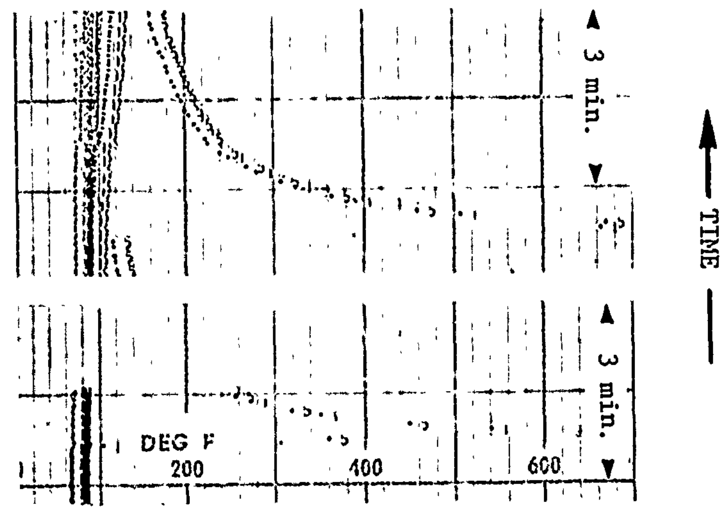
The foregoing analysis of the thermal environment, in which the pressure injection system of a ferrous die casting operation must perform, led to a consideration of how that environment influences the physical dimensions of the system components. That analysis made it quite clear that the task of designing a precision injection system that would maintain small clearances between the plunger and the shot sleeve of a horizontal ferrous die casting machine would be formidable, indeed. If isothermal conditions could be maintained in the shot sleeve, however, the situation would become much simpler.

*These dwell times were subsequently discovered to have been unnecessarily long.



Six seconds

Nine seconds

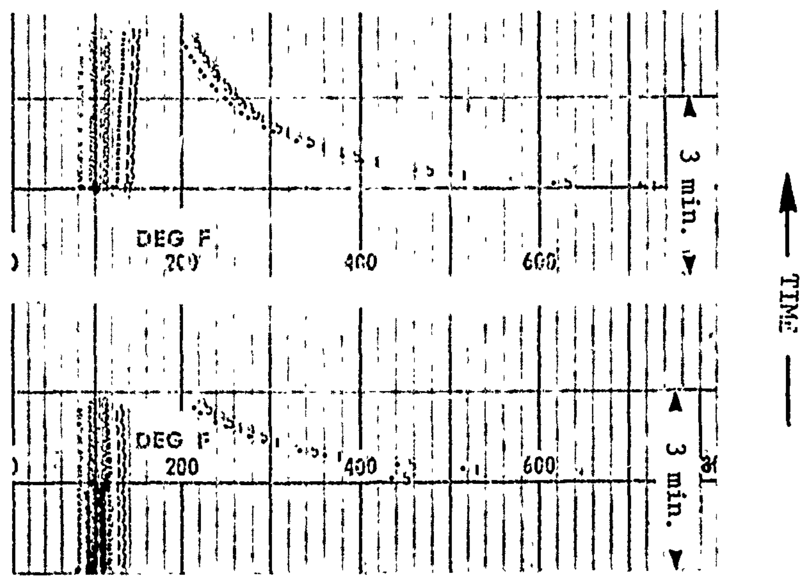


Temperature (°F)
Invar 36 Plunger

Temperature (°F)
Berylco-10 plunger

Six
seconds

Nine
seconds



Temperature (°F)
TZM Plunger

Figure 26 - Recorder chart indication of the thermal response of water-cooled plungers of beryllium-copper, Invar-36, and TZM to contact with a molybdenum block at 2900°F for six and nine seconds.

One approach to the problem of creating an isothermal pressure injection system is to uniformly heat the whole system. Induction heating was selected initially, under the assumption that it would supply a very uniform heat input. A 20 KW Inductotherm feeder panel was used in conjunction with the 100 KW motor generator that also served as a melting power supply. The first trial was conducted on the C-1020 steel shot sleeve previously employed for temperature distribution measurements. The simulated die casting machine (SDCM) was used as a test bed. Surface temperatures were measured with Pyrocon contact thermocouples at both ends and midway between the ends of the sleeve. The results indicated that the induction coil was not heating the sleeve uniformly but was preferentially heating the middle of the sleeve.* End-to-midpoint temperature differences as great as 360°F were observed at a nominal temperature of 1000°F. Figure 27 illustrates the experimental setup.

To rectify this problem, the induction coil was modified by the addition of several taps so that the current might be shunted past the center section of the coil. (See Figure 28.)

In the meantime, a composite shot sleeve was under construction. The survey of potential shot sleeve materials by Dort had selected Rene' 41 as the prime shot sleeve candidate, based on its high elevated-temperature strength and good resistance to thermal shock and oxidation. Rene' 41 was therefore employed as a liner for the composite shot sleeve. A relatively thin-walled liner (1/4") was selected to minimize the resistance to heat transfer. This liner was then backed up by Berylco-10, a moderately strong, beryllium-copper alloy with a relatively high thermal conductivity (0.20 to 0.28 cal $sec^{-1}cm^{-2}{}^{\circ}C^{-1}$ cm), which was intended to serve as a heat source or sink (depending on whether the composite sleeve was heated or not) and as a heat exchange medium. An H-13 casing was added for strength. The construction of this composite shot sleeve is illustrated by Figure 29. A patent application, covering this design concept, has been filed on the behalf of the Moline Malleable Iron Company.

To evaluate this composite shot sleeve, it was first subjected to the same test given the C-1020 shot sleeve. It was heated in the induction coil without shunting the coil, and temperature measurements were made at both ends and midway between the ends. At a nominal temperature of 1000°F, the end-to-midpoint temperature variation was observed to be only 120°F, a marked improvement over the C-1020 shot sleeve.

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*Pyrocon thermocouples are a product of the Alnor Instrument Company, Chicago, Illinois.

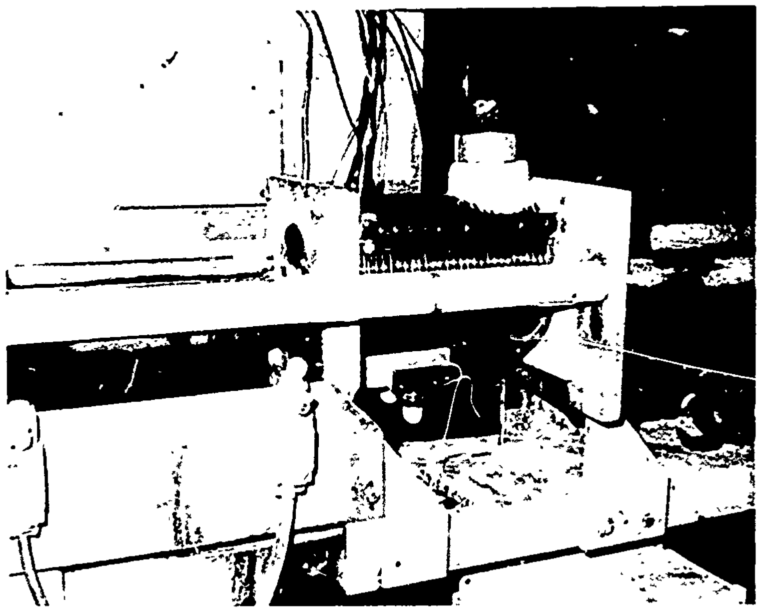


Figure 27 - Photograph of the equipment used to investigate the induction heating of shot sleeves.

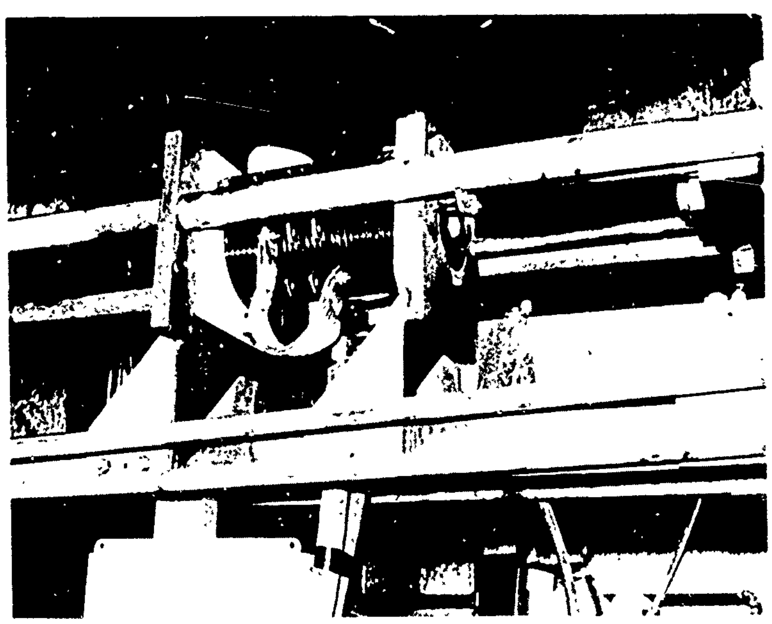


Figure 28 - Photograph of the equipment employed to investigate the use of a shunted induction coil to heat shot sleeves.

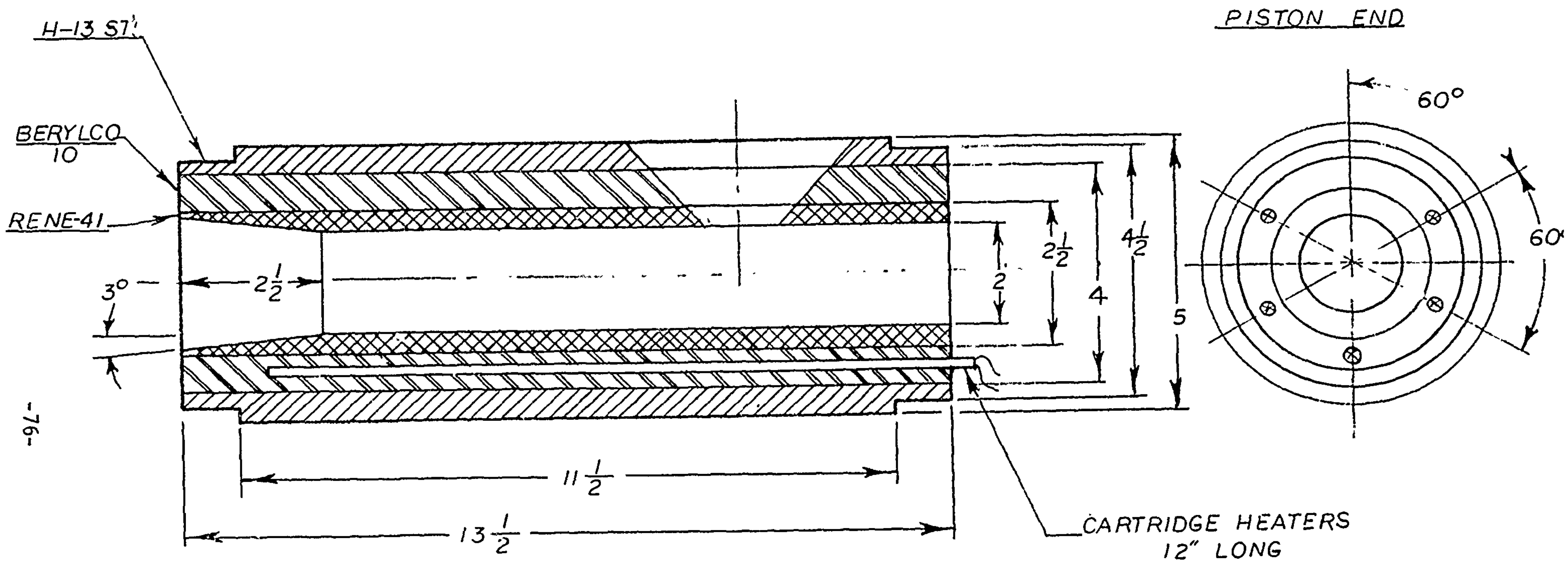


Figure 29 - Sectional and end views of the composite Rene-41, beryllium-copper, H-13 shot sleeve designed by Dort.

with the current shunted past a center section of the induction coil. At a nominal temperature of 1000°F, this modified coil produced an end-to-midpoint temperature variation of only 70°F.

The results of the temperature measurements made on induction-heated shot sleeves are tabulated in Table X.

Although the induction approach to heating shot sleeves has merit, it also creates at least two potential problems:

a. The bulk of an induction coil may be difficult to accommodate on some existing die casting machines

b. Unwanted electromagnetic coupling with other components of the die casting machine may be encountered.

A second approach to heating a shot sleeve was also investigated, using the composite Rene' 41 beryllium-copper H-13 shot sleeve. The approach was to bury cartridge heaters in the beryllium-copper, making it serve as a heat source. Watlow Firerods were employed. Seventeen thermocouples were buried 1/4" away from the inner working surface of the shot sleeve (at the interface between the Rene' 41 and the beryllium-copper), in an arrangement identical to that employed for making temperature distribution measurements on the C-1020 and TZM shot sleeves. One of these thermocouples, near the midpoint of the composite shot sleeve, was electrically connected to a temperature controller. The SDCM was again used as a test bed.

Only static temperature distribution measurements had been made on the shot sleeves heated by the induction coil. To determine the merit of a composite shot sleeve heated by cartridge heaters, however, the sleeve was subjected to simulated die casting conditions. The temperature of the malleable iron melt was maintained at 2600°F. Each shot weighed approximately five pounds. A Berylco-10 beryllium-copper plunger with a 0.001" clearance on the diameter was employed. For each shot, a 1/2" thick graphite wafer with a clearance of 0.003" was inserted ahead of the plunger. The shot sleeve was sprayed with a water suspension of Foseco's XS-1040-B before each shot.

To establish a point of reference, a first test was conducted employing three 1/4" diameter by 12" long cartridge heaters, having a total rated capacity of 2100 watts, to preheat the sleeve. The temperature controller was set at 300°F. Upon reaching temperature,

Table X--Temperature Distribution Data for Shot Sleeves Heated by an Induction Coil

Temperature - Degrees F

Full Coil						Shunted Coil		
C-1020 Steel Sleeve			Rene' 41, Be-Cu, H-13 Composite Sleeve			Rene'41, Be-Cu, H-13 Composite Sleeve		
End	Center	Diff	End	Center	Diff	End	Center	Diff
210	200	10	260	220	40	220	200	20
410	400	10	380	450	70	400	380	20
590	680	90	610	590	20	590	610	20
780	970	190	870	810	60	780	810	30
980	1240	360	1000	1120	120	990	1060	70

the heaters were disconnected, and the SDCM was operated without supplying supplemental heat to the shot sleeve. Figure 30 is a photograph of the chart on which the results were recorded. It illustrates the thermal response of the composite shot sleeve when preheated only. The temperature of the shot sleeve progressively increased for the first six shots, at which time an equilibrium temperature was established. (This equilibrium temperature is a function of the pouring temperature, the shot weight, and the casting rate.)

Having established the performance of the unheated composite shot sleeve as a reference, the test was repeated with the shot sleeve temperature being controlled at 400°F, 600°F, 800°F, and 1000°F. Six cartridge heaters, having a total rated capacity of 7200 watts, were employed to maintain the control temperatures. Figures 31 through 34 display short lengths of recorder chart typifying the thermal response of the composite shot sleeve to simulated die casting when controlled at 400°F, 600°F, 800°F, and 1000°F, respectively.

Excluding Points 1 and 8, which recorded temperatures measured at the die and plunger ends of the shot sleeve, respectively, it may be observed that controlling the shot sleeve temperature at either 600°F or 800°F (Figures 32 and 33) minimizes temperature excursions in the sleeve and creates nearly isothermal conditions. The improvement is particularly obvious when Figures 32 and 33 are compared with Figures 14 and 15, which record, respectively, the response of an unheated C-1020 and an unheated TZM shot sleeve to simulated ferrous die casting.

A second approach to creating an isothermal metal injection system is to very effectively remove the heat transferred from the molten shot to the shot sleeve. Following this approach, Dort designed an air-cooled shot sleeve consisting of a Rene' 41 liner inside a circumferentially finned aluminum tube. This concept, which is the subject of a patent application filed on the behalf of the Moline Malleable Iron Company, is illustrated in detail by Figure 35.

To test the concept, an air-cooled shot sleeve was fabricated by the Dort Metallurgical Company. To evaluate the thermal response of such a sleeve, instrumentation identical to that used on the composite sleeve was added. The shot sleeve was heated to a nominal temperature of 250°F with a gas heater. The simulated die casting operation was identical to that conducted to evaluate the composite shot sleeve.

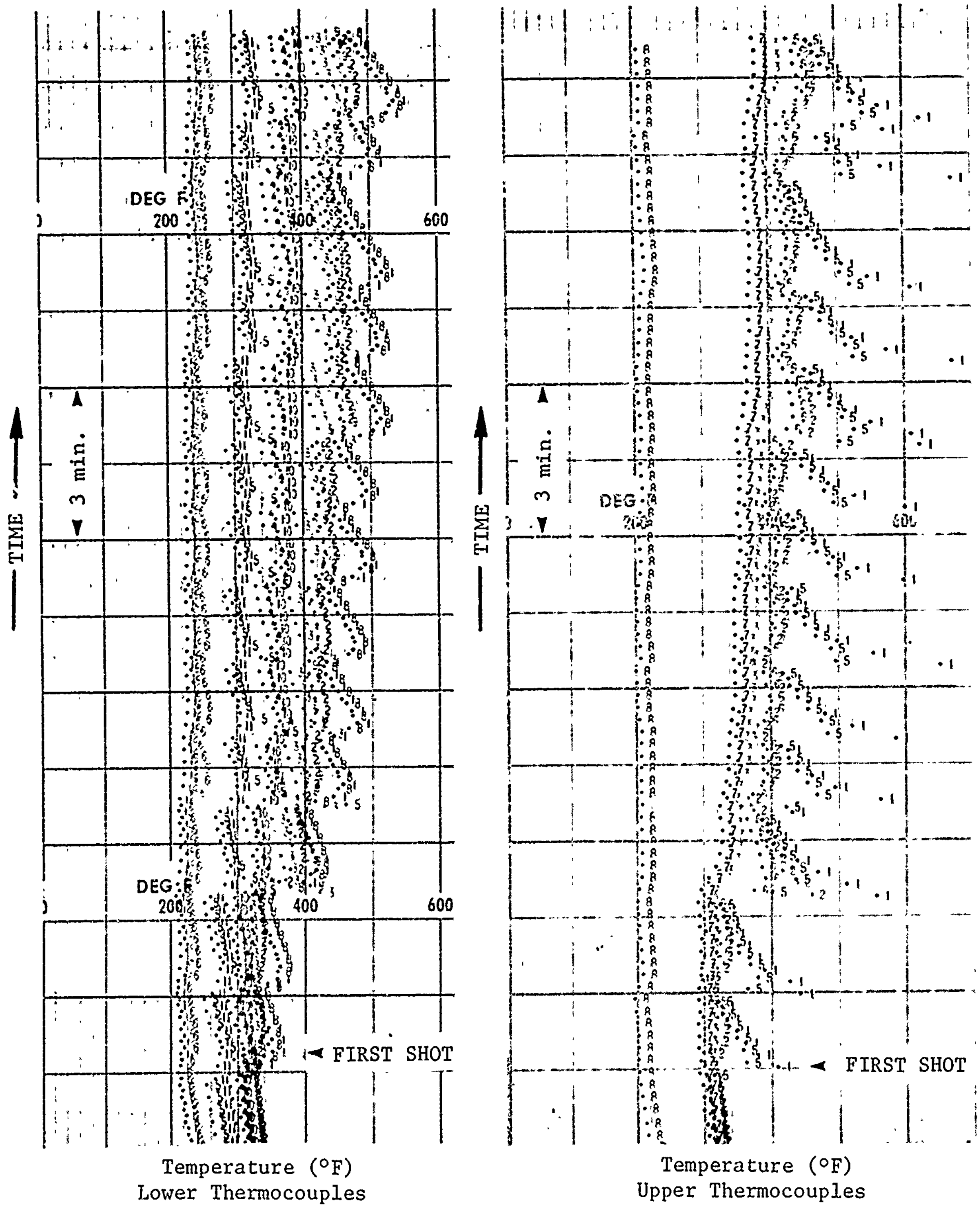


Figure 30 - Recorder chart indication of the thermal response of an unheated, composite, Rene-41, beryllium-copper, H-13 shot sleeve to simulated die casting.

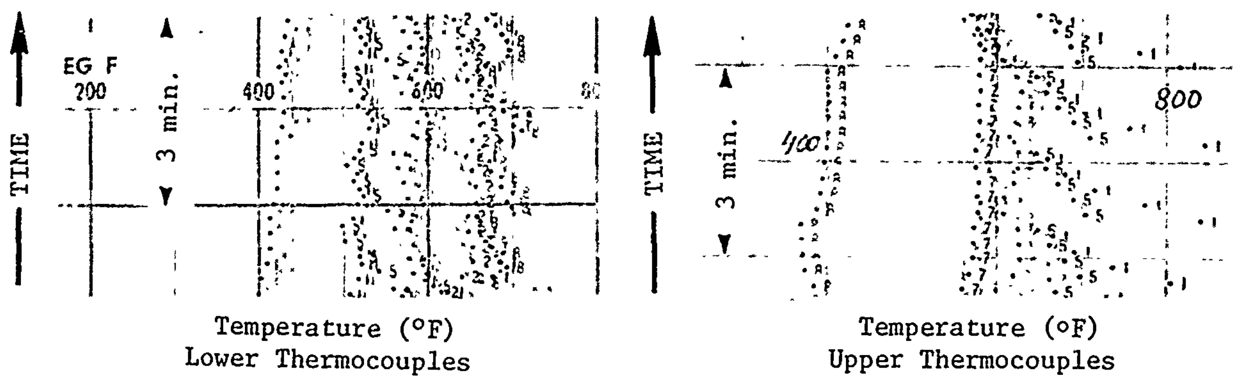


Figure 31 - Recorder chart indication of the typical thermal response to simulated die casting of a composite shot sleeve maintained at 400°F.

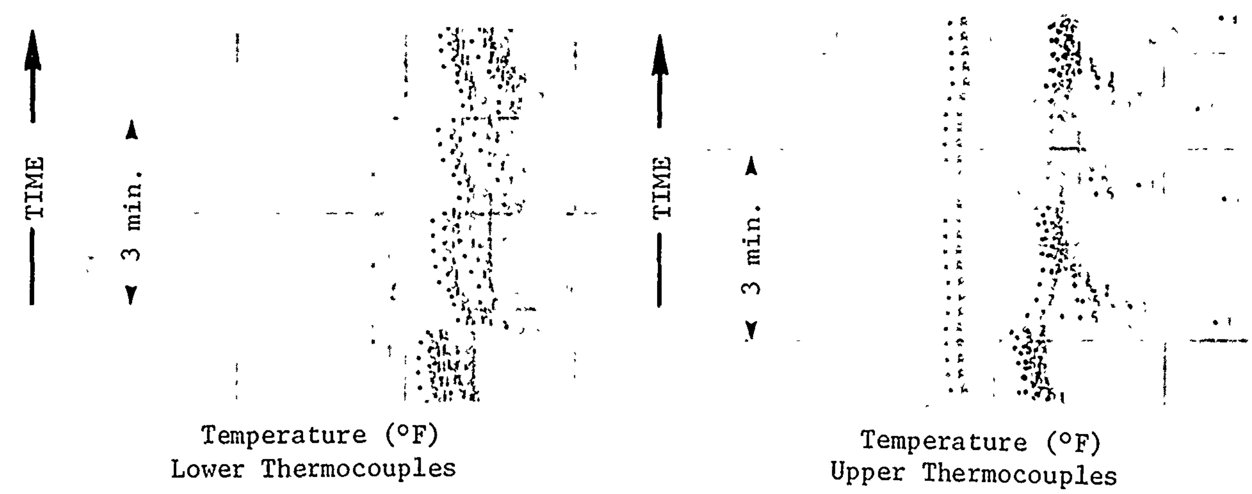


Figure 32 - Recorder chart indication of the typical thermal response to simulated die casting of a composite shot sleeve maintained at 600°F.

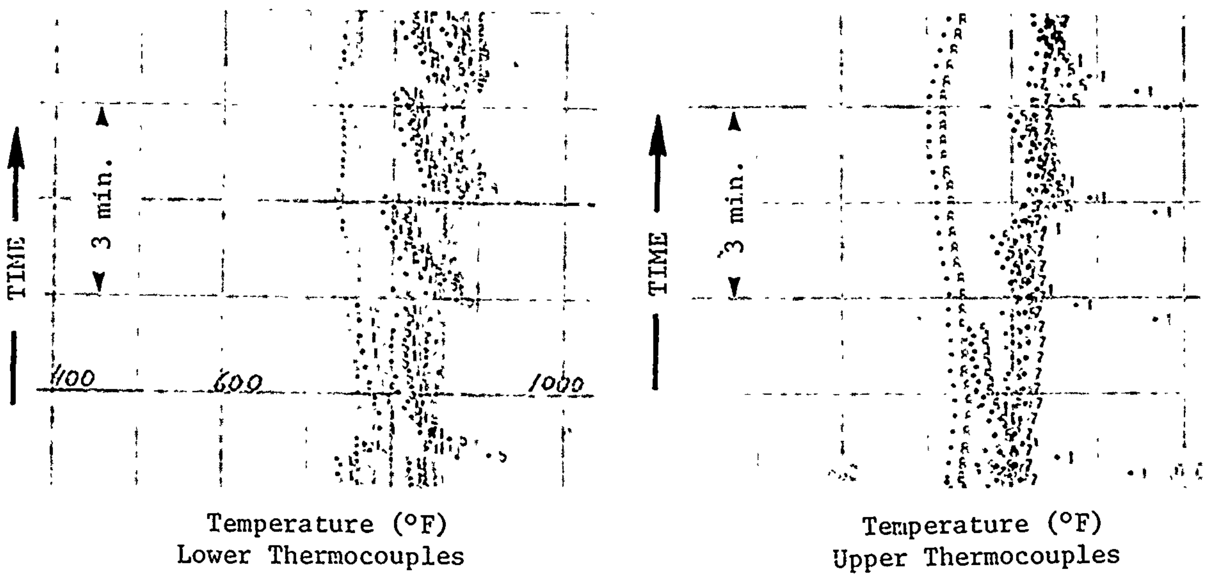


Figure 33 - Recorder chart indication of the typical thermal response to simulated die casting of a composite shot sleeve maintained at 800°F.

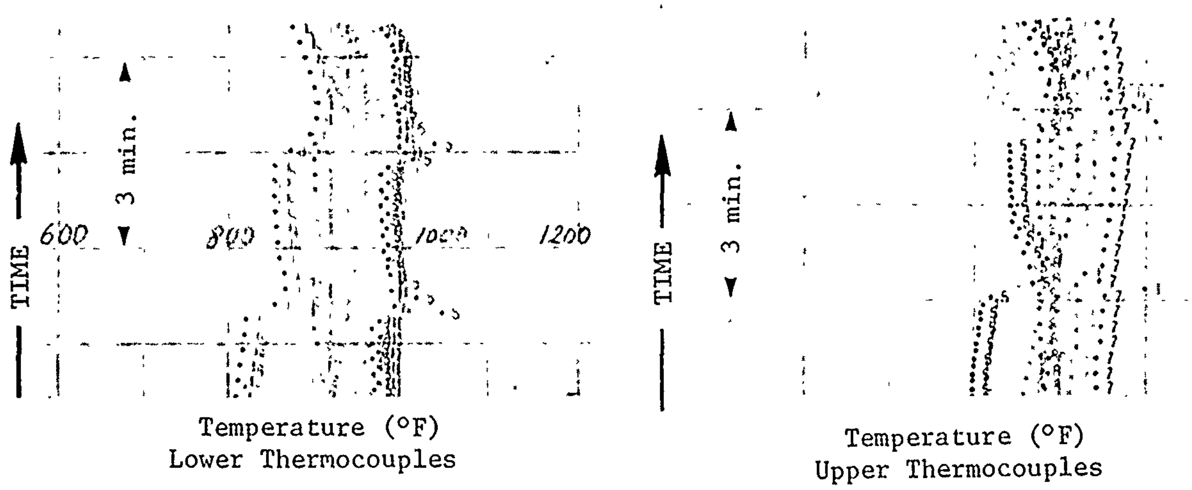


Figure 34 - Recorder chart indication of the typical thermal response to simulated die casting of a composite shot sleeve maintained at 1000°F.

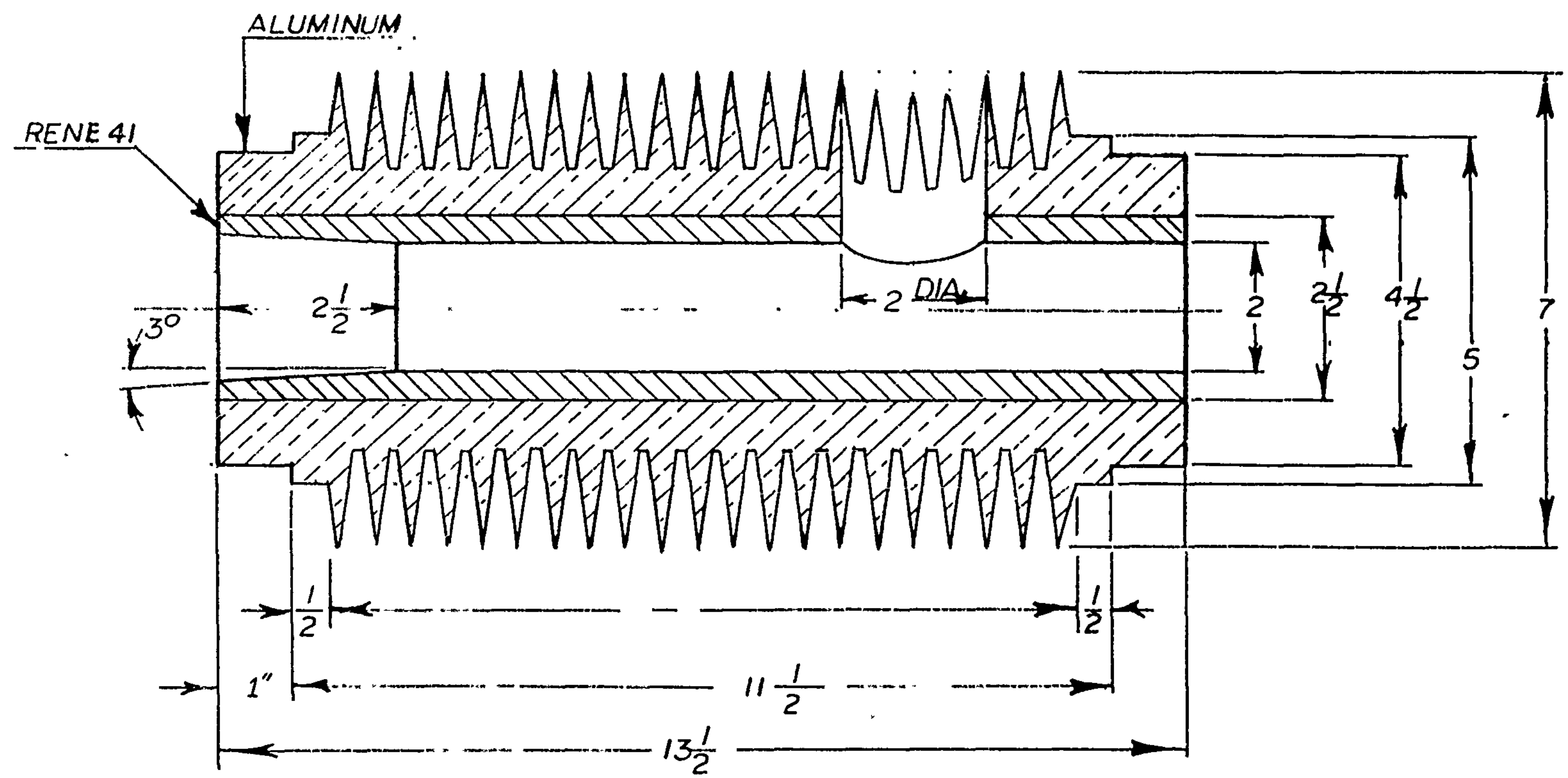


Figure 35 - Sectional view of an air-cooled shot sleeve designed by Dort.

Several shots were made which exhibited a maximum local temperature rise of approximately 80°F at Position 4, directly beneath the pour hole. In an effort to limit this temperature rise, forced air cooling was applied to the outside of the sleeve in the area opposite the pour hole. As the simulated die casting proceeded, the forced air cooling was effective in gradually limiting the maximum local temperature rise, as indicated by the photograph of the recorder chart in Figure 36. Points 1 and 5 correspond to two thermocouples buried in the die end of the shot sleeve. They may be disregarded.

A comparison of Figures 32, 33, and 36 indicates that more nearly isothermal conditions were realized with the air-cooled shot sleeve than with the composite shot sleeve. The air-cooled shot sleeve possesses the additional virtue of ease of operation.

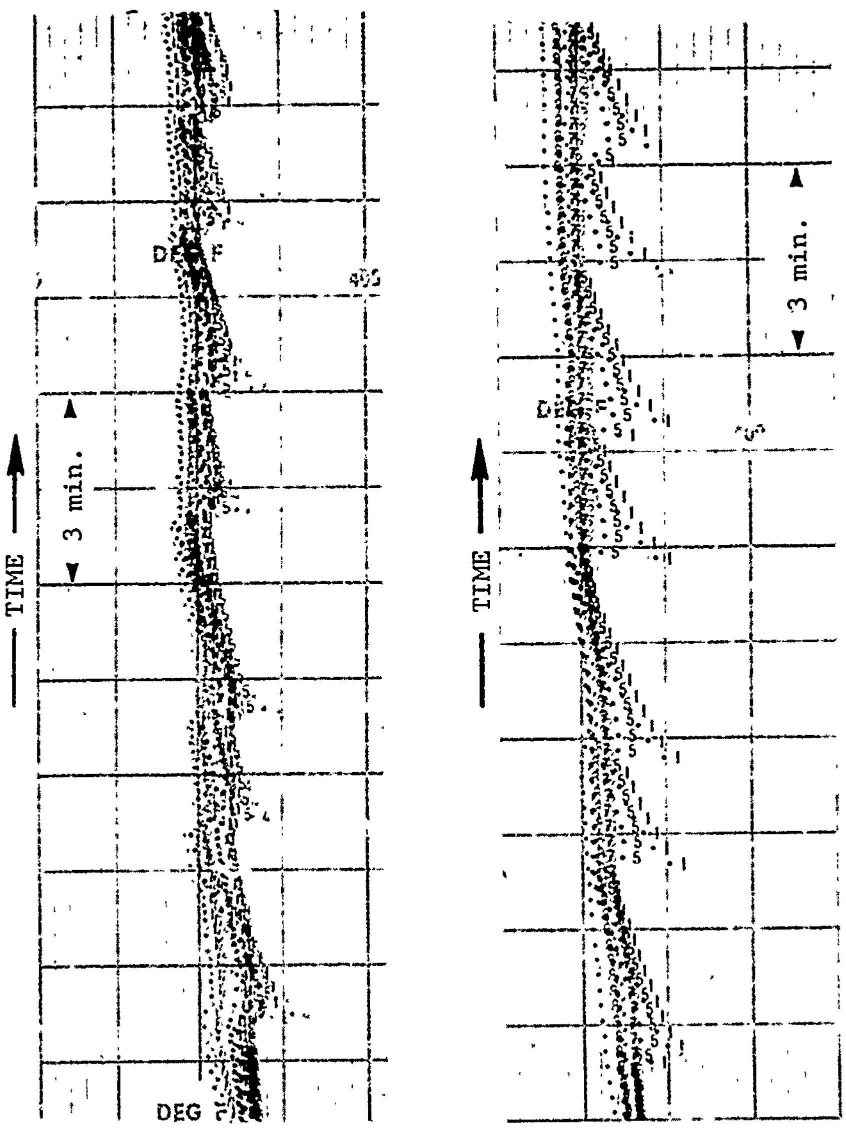
To design a metal injection system that would provide the desired clearances between the plunger and the shot sleeve at the operating temperature, it was necessary to have reliable thermal expansion data. The TZM and Invar-36 plungers had been selected for evaluation on the basis of the low values of thermal expansion reported for these alloys in the literature. To verify these values and those for Berylco-10, a plunger of each material was heated, in turn, in a laboratory furnace to slightly over 1000°F, and then permitted to cool. The diameter of each plunger near the plunger face was measured with a micrometer at preselected temperatures between 1000°F and room temperature. (No compensation was made for the effect of heat on the micrometer.) The results are tabulated in Table XI.

Similar data for shot sleeves were also required to design a metal injection system with desirably small clearances; and for the composite shot sleeve in particular, reliable values could not be calculated with a high degree of confidence. Therefore, an effort was made by Dort to generate these data.

The induction coil was employed to heat the C-1020 sleeve and the Rene' 41, beryllium-copper, H-13 composite sleeve. A 2-1/4" long hole was drilled in the face of each sleeve, parallel to the axis. A thermocouple, inserted in that hole, was employed to measure the temperature of the sleeve.

The diameter of the air-cooled shot sleeve was too great to permit it to be heated in the induction coil. Accordingly, it was heated in a laboratory furnace.

Micrometer measurements were made of the internal diameter of the shot sleeve as close as possible to the end of the sleeve and 2-1/4" from the end at predetermined temperatures. The results are tabulated in Table XII.



Temperature (°F)
Lower Thermocouples

Temperature (°F)
Upper Thermocouples

Figure 36 - Recorder chart indication of the thermal response of the air-cooled shot sleeve to simulated die casting.

Table XI--Thermal Expansion of Plungers at Elevated Temperatures

Temperature °F	Diameter in Inches Berylco-10	TZM-Moly	Invar-36
Room Temperature	1.999	1.996	1.996
200	2.002	--	--
300	2.004	1.997	1.996 (350°F)
400	2.007	1.998	1.997
500	2.009	--	1.998
600	2.011	1.999	2.000
700	2.013	2.000	2.003
800	2.016	2.001	2.005
900	2.018	--	2.006
1000	--	2.002	2.008

Table XII--Thermal Expansion of Shot Sleeves at Elevated Temperatures

Temperature °F	Inside Diameter in Inches		
	C-1020 Shot Sleeve	Rene' 41, Be-Cu, H-13 Composite Shot Sleeve	Air-Cooled Shot Sleeve
Room Temperature	2.027	1.996	1.998
200	--	1.997	1.999
250	2.028	--	--
300	--	--	2.001
400	--	2.002	2.002
450	2.030	--	--
500	--	--	2.003
600	--	2.006	--
700	2.032	--	--
800	--	2.010	--
950	2.033	--	--
1000	--	2.013	--

Figure 37 graphically illustrates all the thermal expansion data generated, both for the shot sleeves and the plungers.

Of the protective coatings evaluated by Dort in the immersion test, the flame-sprayed ZrO_2 showed the least deterioration, and thus appeared to offer the most promise for protecting shot sleeves used in a ferrous die casting operation. Consequently, a uniform 0.015" ZrO_2 coating was sprayed on the inner diameter of a Rene' 41 shot sleeve liner by the Metallizing Company of America. The coated sleeve was then subjected to a grinding operation to produce a smooth surface.

The coated Rene' 41 liner was evaluated in use in a ferrous die casting operation on Dort's 400 ton Lester* horizontal die casting machine. A beryllium-copper plunger, 0.010" smaller than the shot sleeve, was employed in the test.

The coating started to disintegrate during the first shot, and the fragments interfered with the movement of the plunger. Very little of the coating remained after the fourth shot. Thus, it was obvious that the coating-substrate bond was not sufficiently strong to withstand the mechanical stresses occurring during operation of the shot sleeve. It is not known if this was due to a poor coating application or to the intrinsic weakness of the coating-substrate mechanical bond.

A completely different approach to the problem of designing a pressure injection system was also evaluated by Dort. The approach was to fabricate the system from Al_2O_3, which had previously been identified as the outstanding ceramic for that application. Thermal shock was predicted to be the most troublesome problem encountered, as a result of substituting a ceramic for the metallic components of the pressure injection system. To circumvent that problem, however, Dort proposed a design enabling the shot sleeve to be preheated and maintained at temperature during the die casting operation.

The design proposed by Dort is illustrated by Figures 38 through 40. The dimensions are based on using the sleeve on the 400 ton Lester machine. As reported earlier, Hycor TA-509, a slip-cast grade of Al_2O_3, had been selected from the many grades available for its resistance to thermal shock and abrasion. The flanges, draft, taper, and inlet orifice of the sleeve were to be incorporated during the casting process, thus resulting in a minimum of machining after the part had been fired. (Fired ware can, however, be machined when needed, using conventional grinding methods.)

*Manufactured by the Lester Engineering Company, Cleveland, Ohio.

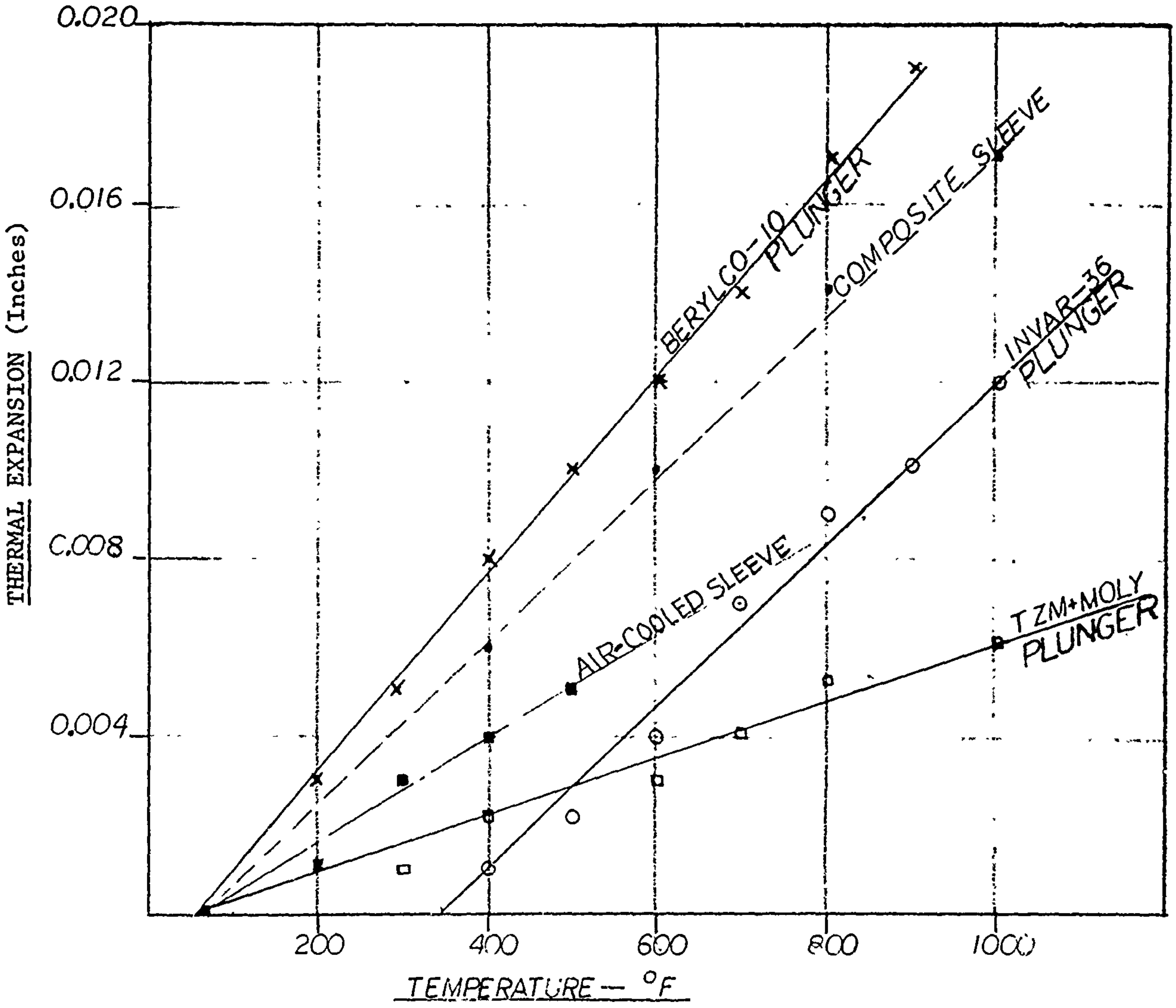


Figure 37 - Thermal expansion of shot sleeves and plungers at elevated temperatures.

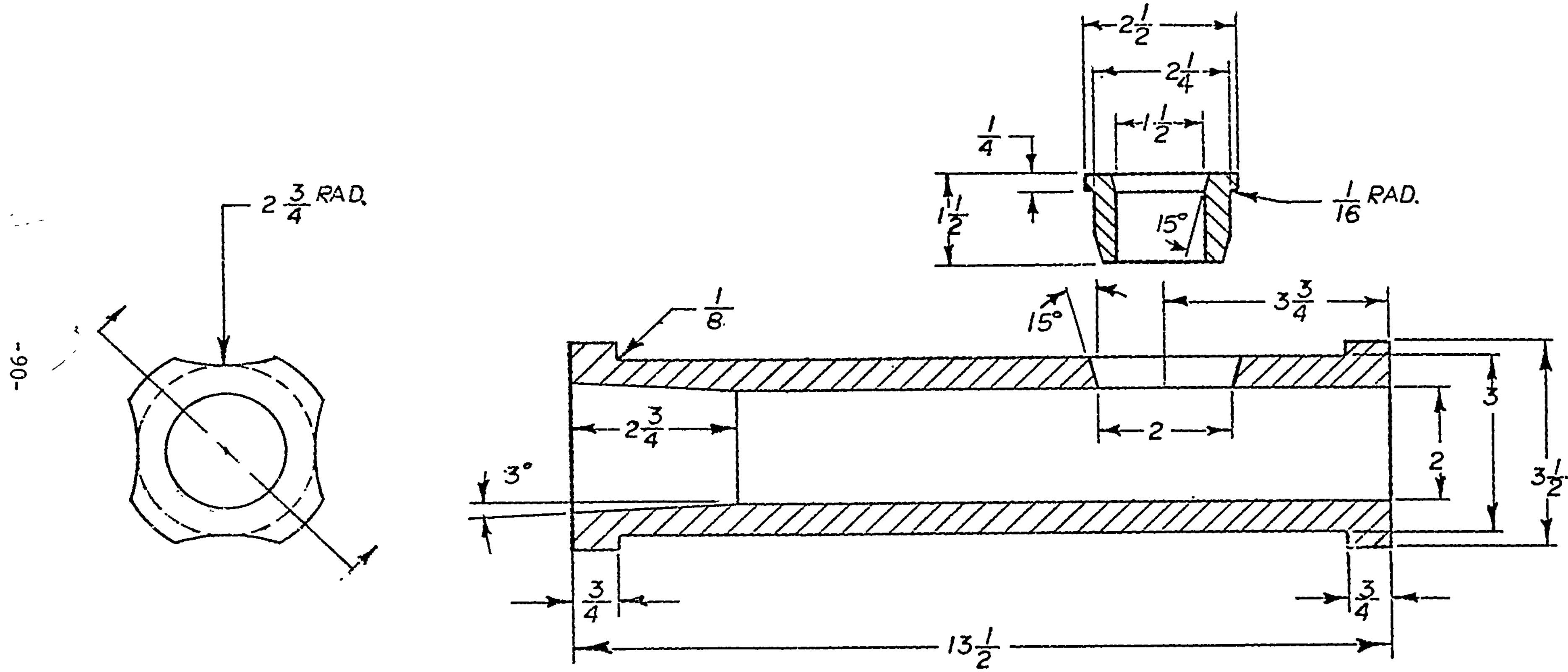


Figure 38 - Design for a ceramic shot sleeve with a pour-hole extension.

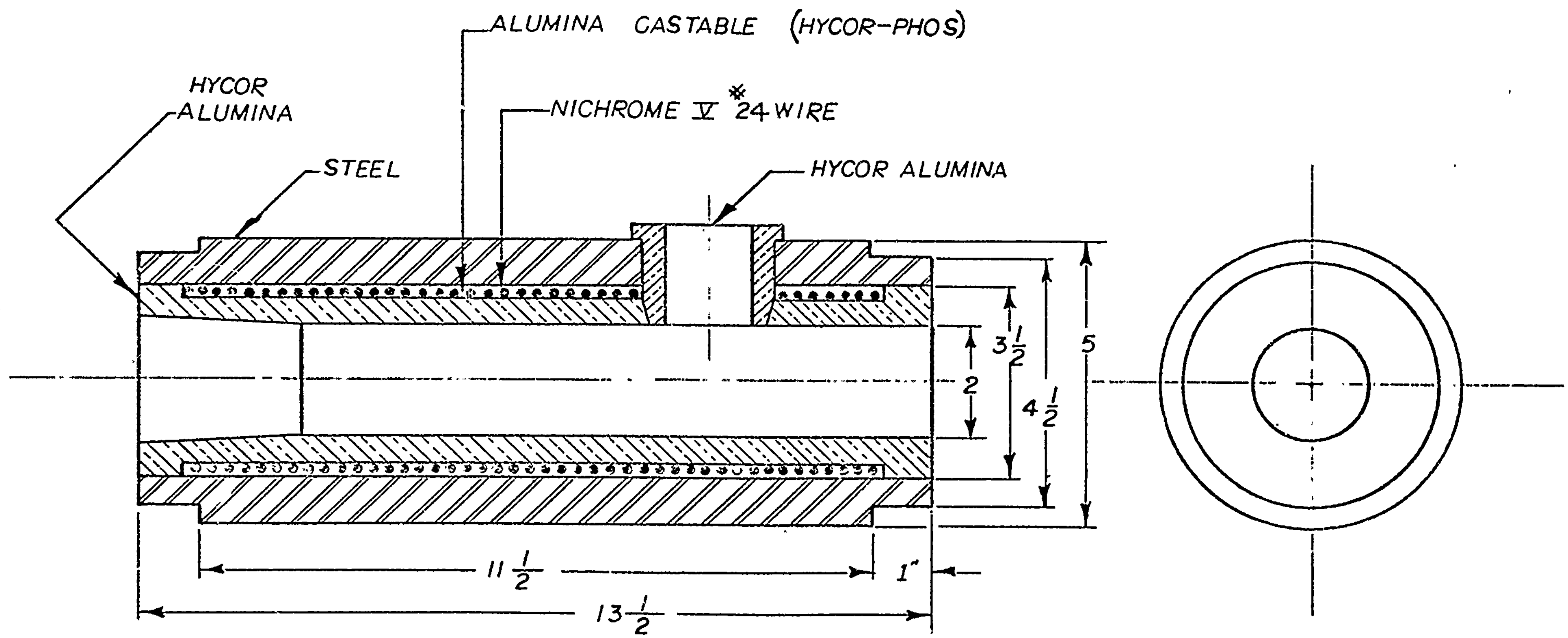


Figure 39 - Design for a ceramic, shot-sleeve assembly.

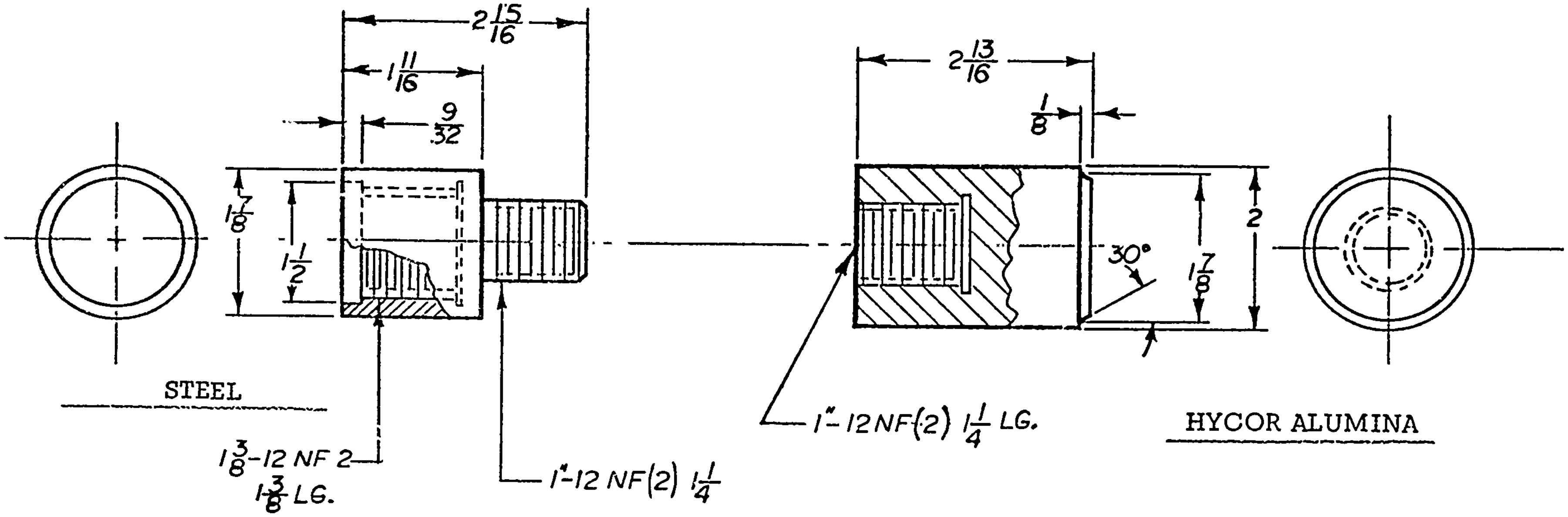


Figure 40 - Design for a ceramic plunger and a steel adaptor

As illustrated in the shot sleeve assembly drawing, Figure 39, the ceramic sleeve would be supported by an external shell of H-13 hot-work die steel. A spiral winding of Nichrome V would be employed to heat the sleeve to 1800°F to 2000°F. An alumina-phosphoric acid, castable cement, would be employed to isolate the windings, to provide thermal isolation for the steel shell, and to bond the sleeve to the shell. A final diamond grinding operation would be employed to assure the concentricity of the outer diameter of the steel shell and the inner diameter of the ceramic sleeve.

Figure 40 illustrates the ceramic plunger and steel adapter envisioned for this system. The plunger was to be cemented to the adapter; but it would also be provided with internal threads, so that it might be screwed on the adapter. It was hoped that this threaded joint would prevent separation of the plunger and adapter, should unexpected drag be encountered during retraction of the plunger.

An Al_2O_3 shot sleeve and plunger assembly was constructed and put on test by Dort. Early in the testing program, the plunger assembly failed, with the alumina plunger becoming separated from the steel adapter. Examination revealed that fracture had occurred, not in the cement bond, but in the alumina plunger itself. Microscopic examination further revealed that the alumina had also suffered some deterioration in the region exposed to the molten metal. Figure 41 compares the microstructure of the alumina, as received, to the microstructure of the plunger face after exposure to molten malleable iron. The white, porous, coarse-grained structure of the material, as received, appears to have undergone a general darkening. Magnetic tests revealed the presence of iron particles in the structure, indicating that molten malleable iron had probably impregnated the porous structure of the alumina. An X-ray diffraction analysis of the impregnated region, however, failed to uncover any indication of a chemical reaction leading to the formation of new compounds or alloys.

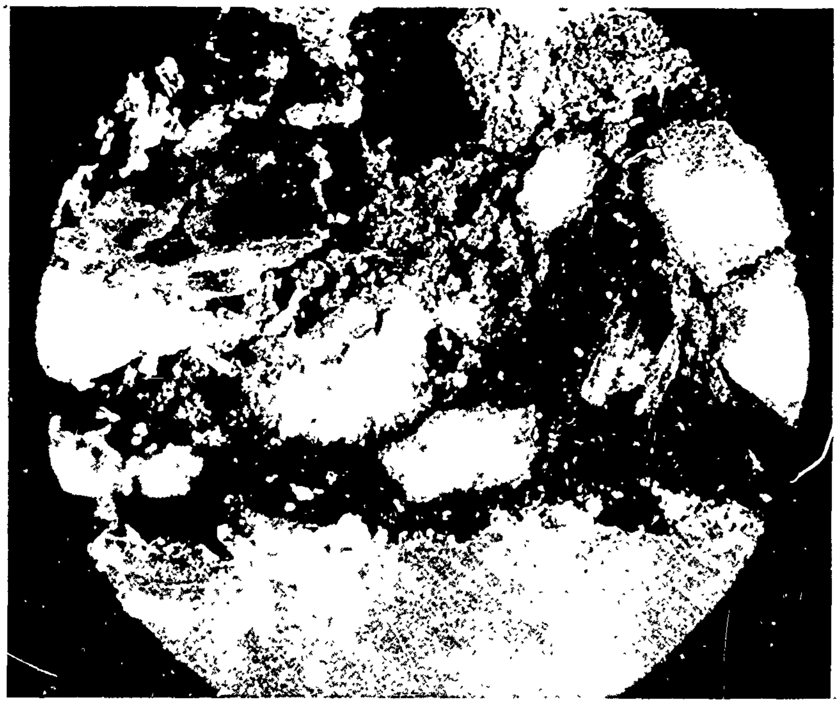
Examination of the sleeve revealed no deterioration. (The apparent superiority of the sleeve may be attributable to a lower volume fraction of voids and/or a lower percentage of connected porosity.)

Continuing the evaluation, an uncooled TZM molybdenum plunger with a clearance of 0.005" was substituted for the alumina plunger. The plunger and the shot sleeve were lubricated with a graphite aerosol, and the system was heated to a temperature of approximately 500°C. The simulated die casting proceeded uneventfully. The test was terminated and the shot sleeve liner was re-examined. No signs of deterioration or interaction with the molten metal were observed.

A patent application covering the concept of a ceramic-lined shot sleeve has been filed on the behalf of the Moline Malleable Iron Company.

As-Received

Mag. = 100X



Plunger -- after exposure to molten metal in the simulated die casting operation. Note the general darkening of the structure. The gray area at the bottom is mounting material.

Mag. = 100X

Figure 41 - Microstructure of Hycor TA-509 Alumina.

The phase of this project related to the evaluation of die materials will be reported in detail in Sections V and VII. That evaluation, however, also afforded the Lamp Metals and Components Department the opportunity to gain extensive experience with a pressure injection system for ferrous die casting. As noted earlier, the system adopted by the Lamp Metals and Components Department employed inexpensive, easily replaceable low-carbon steel or low alloy steel shot sleeve liners, which extended almost to the parting face of the cover die and formed an annular interface with the cover die. Eventually, that interface would inevitably open up, forming a gap; and the tip of the shot sleeve liner would inevitably become necked down: Initially, the Lamp Metals and Components Department had sprayed a ceramic refractory coating in the form of a water suspension into the bore of the shot sleeve liner. The purpose of the coating had been to protect the shot sleeve liner and augment the seal formed between the liner and the plunger tips. The overspray, however, had also prevented flash from penetrating the interface between the cover die and the shot sleeve liner. Elimination of that spray had permitted the formation of a gap at the interface between the shot sleeve liner and the cover die to proceed much more quickly.

It will be noted that the formation of gaps at interfaces between die components that are exposed to molten metal is a very general phenomenon in ferrous die casting. The mechanism is thought to be as follows:

a. During every cycle, the surfaces of die components exposed to liquid metal attempt to expand; they are constrained from doing so, however, by the underlying material

b. At an interface, that self-constraint is unsymmetrical; as the components on either side of an interface attempt to expand, they are partially constrained by the component on the other side of the interface, which is also compelled to expand; the result is that both components may be plastically deformed or upset, forming a gap at the interface

c. On subsequent cycles, molten metal penetrates that gap and solidifies, while the surface temperature of the components on either side of the gap is still increasing; the flash acts as a wedge, permitting the mechanism described in b, above, to act repetitively

d. There is also a distinct possibility that the walls of the gap may be broached or abraded by the flash formed in the gap when the casting is ejected.

The material properties required to avoid gap formation are identical to those required for resistance to thermal fatigue, with one exception--high ductility is not required. The properties that are required are high heat diffusivity, low thermal expansion, high strength, and a low elastic modulus. This combination of properties would minimize the temperature rise at the surface of the die, minimize the resulting strain, and maximize the elastic component of that strain.

Filling an already formed gap with a resilient material or (as in the case of the ceramic spray) filling the gap repetitively with a crushable material will also retard gap formation, if the material filling the gap excludes molten metal. With nothing to fill the gap, the tip of the shot sleeve liner becomes virtually surrounded by molten metal during each injection cycle. The result is that the tip of the shot sleeve liner must endure unduly high temperatures and unduly great thermal cycles.

The experience of the Lamp Metals and Components Department indicated that the increased temperatures to which the tips of the shot sleeve liners were subjected after the ceramic spray had been eliminated created concomitantly greater strains in the liners. The increased temperatures also reduced the strength and elastic modulus of the liners. Because a decrease in the elastic modulus would reduce the plastic component of strain, whereas a decrease in strength would increase the plastic component of strain, no a priori conclusion can be reached concerning the net effect of these two counter trends. It is clear, however, that increasing the temperature of the tip of the shot sleeve liner increased its vulnerability to soldering. Efforts to eject biscuits that had unexpectedly soldered to the shot sleeve liner or were only mechanically stuck in the liner as a result of earlier necking down appeared to contribute to further necking of the liner. This resulted from what was termed the "rubber tube mechanism" (i.e., a tensile load on the shot sleeve liner causing the liner to increase in length but decrease in diameter).

Throughout most of the die materials evaluation, the Lamp Metals and Components Department used annealed AISI 4130 steel shot sleeve liners. Typically, these liners had to be replaced after fewer than 500 shots because their tips had become necked down. The decrease in the life of shot sleeve liners, associated with the elimination of the ceramic, prompted the Lamp Metals and Components Department to

look at alternate materials. One of those materials which was selected on the basis of its reportedly high hot hardness was a tool steel referred to as Molex 7.* It was evaluated in both the spheroidized annealed condition (with an approximate hardness of 216 BHN) and with the tip only hardened to 58.6 to 59.8 R_C (627 to 653 BHN). Regardless of their condition, the Molex 7 liners performed no better than the conventional AISI 4130 liners. On the basis of a single comparison, it appeared that a Molex 7 liner with a hardened tip would provide longer life than an annealed Molex 7 liner, even though appreciable softening was noted in the hardened tip during the course of its service (186 cycles). Table XIII indicates the effect of tempering temperature on the hardness and impact values of Molex 7.

At the same time the Molex 7 was being evaluated, the Lamp Metals and Components Department was also investigating the possibility of providing supplemental cooling for the tips of shot sleeve liners. Air cooling was selected as the simplest approach. The bore of the hole in the cover die that accepted the shot sleeve liner was grooved to form an annular plenum. To get the air in and out, holes were drilled to the plenum from either side of the cover die parallel to the parting face. The results of the experiment were not encouraging. Even at low pressures, air leaked from the groove into the die and blew molten metal back out the pour hole. The effort to provide supplemental cooling was abandoned.

Wrought molybdenum was another alternate material evaluated as a shot sleeve liner by the Lamp Metals and Components Department. It was selected on the basis of good hot strength, very low thermal expansion, and very high heat diffusivity. The single molybdenum shot sleeve liner that was evaluated by the Lamp Metals and Components Department was machined from a rolled, stress-relief annealed, unalloyed molybdenum bar. It served continuously, from the time it was installed until the die materials evaluation was terminated, 925 cycles later. The molybdenum liner evidenced much less tendency to become necked down than the steel shot sleeve liners had, but the bore of the liner became badly worn with continued service. The liner had originally been honed to an inside diameter of 0.998" to 1.000". When the die materials evaluation was terminated, the inside diameter of the molybdenum liner was observed to have increased to 1.018" to 1.023" at the die end. Approximately 2" from the die end, the diameter was observed to be 1.008" to 1.025". A single measurement made on the die side of the pour hole indicated the diameter to be 1.009" at that point; several measurements made on the plunger side of the pour hole resulted in values of 1.009" to 1.040". Doehler Jarvis had

*Nominal composition: 0.76% carbon, 0.78% manganese, 0.93% chromium, 0.38% molybdenum, 1.85% nickel, 0.015% sulfur (maximum), 0.015% phosphorus (maximum), balance iron. Supplied by the Associated Steel Corporation, Cleveland, Ohio.

Table XIII--Response of
Quenched Molex 7 to Tempering*

Tempering Temperature (°F)	Hardness (R_C)	Charpy Impact Strength (ft-lbs)
300	62	19.0
400	61	39.9
500	57	80.0
600	56	88.4
700	53	92.5
800	50	102.7
900	48	102.7
1000	46	104.7
1200	35	167.4

*Austenitized at 1525°F, quenched in 150°F oil.

concluded that high-density, pressed and sintered molybdenum was too soft to be useful as a shot sleeve. The results reported here indicate that, in spite of the strain hardening affected by the warm rolling process, wrought molybdenum, too, has insufficient wear resistance to be useful as a shot sleeve liner for a precision pressure injection system.

2. VERTICAL MACHINE

Both the Doehler Jarvis Division of the National Lead Company and the General Electric Company Research and Development Center used vertical die casting machines to perform the ferrous die casting required to fulfill their individual responsibilities under the contract.

Doehler Jarvis' approach to the pressure injection of ferrous metals was influenced by their die casting experience with non-ferrous metals. Following their standard practice, the injection system which Doehler adopted employed a closely fitted beryllium-copper plunger (i.e., less than 0.001" clearance between the plunger and the shot sleeve). The innovation which distinguished the pressure injection system which Doehler adopted for ferrous die casting from the systems which they use for non-ferrous die casting was the use of a collapsible, sacrificial, insulating cup inserted in the shot sleeve prior to each shot to contain the molten metal. The purpose of that cup was to protect the shot sleeve and the plunger from the ravages of the molten metal, thereby maintaining the fit of the plunger and prolonging the life of the systems. In addition, the insulating cup was intended to prevent the molten metal from solidifying in the shot sleeve during the time required to retract the hand ladle, close the die, and inject the metal. From another perspective, Doehler's approach had the added advantage of permitting the metal to be injected at a lower temperature than would have otherwise been possible, thereby reducing the probability of soldering, reducing the maximum temperature and the thermal cycle to which the die was exposed, and reducing the shrinkage that the castings experienced. Therefore, it was anticipated that the net effect of enabling ferrous metals to be die cast at lower temperatures would be to prolong die life.

Doehler also evaluated several alternate shot sleeve materials.

After a long and futile effort to find a vendor for an insulating cup, Doehler decided to make the cups in-house. They were fabricated from asbestos paper glued with waterglass (sodium silicate). It was necessary to remove the water from the waterglass and to remove the water of hydration from the asbestos to prevent inordinate outgassing

when the cups were filled with molten metal. Initially, 1300°F was selected as an appropriate drying temperature for the cups, but it was later determined that drying at 1800°F for one hour was necessary to completely eliminate manifestations of outgassing.

The drying treatment reduced the strength of the asbestos cups, making them very fragile. Considerable care had to be exercised when inserting the cups into the shot sleeve. Once in place, however, they were effectively supported by the shot sleeve and the plunger.

The cups proved to be very effective in protecting the injection system from the molten ferrous alloys. They had a number of disadvantages, however. First, although it was only related to the materials selected and not to the concept itself, the discovery of flake-like inclusions of asbestos in the castings represented a problem that could not be tolerated by anyone attempting to produce high quality, ferrous die castings. More fundamentally, the insulating cups represented added expense, both as a consumable supply and also because their insertion represented added cycle time. The fact that the cups so effectively insulated the biscuits was, to some extent, a disadvantage, too. The effective insulation of the biscuit forced Doehler to employ somewhat longer dwell times than would otherwise have been necessary, simply to avoid encountering biscuit explosions. Again, this represented additional cost in the form of increased cycle time. Although the problem will be discussed again later in this report, it is important to note at this point that longer dwell times also increase the probability of die damage. This is so because the castings are permitted to shrink further, cool more, and attain higher strength and hardness values while still in the die. Shrinking castings can deform die cavities and destroy detailed die features; and the stronger the castings are, the worse the problem becomes. In addition, the abrasion experienced by the die when ejecting castings that have shrunk onto projections in the die becomes more severe as the hardness of the castings increases. The effects of increased dwell time are also fundamental disadvantages related to the use of insulating cups in the pressure injection system of the ferrous die casting process. Finally, die casters normally hope to recycle their scrap. The remnants of an insulating cup may well present scrap conditioning problems and/or slag removal problems and associated expenses.

In their early casting runs, Doehler employed a high-density, pressed and sintered molybdenum shot sleeve and a water-cooled, beryllium copper plunger. The clearance between the plunger and the shot sleeve was less than 0.001". As this system accumulated service, it was observed that the inner diameter of the molybdenum shot sleeve was progressively increasing

at the point of entry into the die, "bell-mouthing." Eventually, this enlargement permitted an iron chip to become wedged in the annulus between the shot sleeve and the plunger, severely scoring both and causing the plunger to seize in the sleeve. The results are illustrated by Figures 42 and 43. Doehler concluded that the strength and hardness of high-density, pressed and sintered molybdenum were not adequate for shot sleeve applications.

After being unsuccessful in their efforts to employ high-density, pressed and sintered molybdenum as a shot sleeve, Doehler had a sleeve made from hardened and nitrided AISI H-13. To accommodate the expansion difference between that sleeve and the high-density, pressed and sintered molybdenum cover half of the die, the sleeve was fabricated with a shoulder or flange at its upper end, which rested flat on the surface of the cover die. The shot sleeve was also provided with a conical tip which, when seated in the corresponding recess machined in the ejector die, insured positive alignment of the sleeve. That design feature which permitted the use of loosely fitting shot sleeves was referred to as a self-cleaning joint by Doehler Jarvis. A patent application was made on the behalf of the Doehler Jarvis Division of the National Lead Company covering the concept of the self-cleaning joint. The concept is illustrated by Figure 44. The performance of the H-13 shot sleeve protected by the insulating cup was satisfactory.

Later in the program, Doehler Jarvis evaluated a nitrided, AISI D-5 steel shot sleeve having a hardness of Rockwell C 60.* The conditions under which the evaluation was performed were as follows:

Cast Metal	AISI 304 Stainless Steel
Metal Temperature	2910°F - 3060°F
Pressure on Metal	24,000 psi
Injection System Protection	Asbestos Cups

After only five injection cycles, a 4" longitudinal crack was observed to have formed through the wall of the D-5 shot sleeve, in spite of the protection provided by the asbestos cup. Therefore, Doehler concluded that AISI D-5 was not a promising material from which to construct shot sleeves for ferrous die casting, presumably because its resistance to thermal shock is inadequate.

The injection system employed by the Research and Development Center is illustrated very well by Figure 1. A two-piece shot sleeve liner, bisected by a plane perpendicular to the axis of the liner, was employed.

*Nominal composition: 1.5% carbon, 12% chromium, 1% molybdenum, 3% cobalt, balance iron.

Figure 42 - Photograph of a high-density, pressed and sintered molybdenum shot sleeve which became badly scored in service at Doehler Jarvis.

Figure 43 - Photograph of a badly scored, beryllium-copper plunger which seized in the high-density, pressed and sintered molybdenum shot sleeve at Doehler Jarvis.

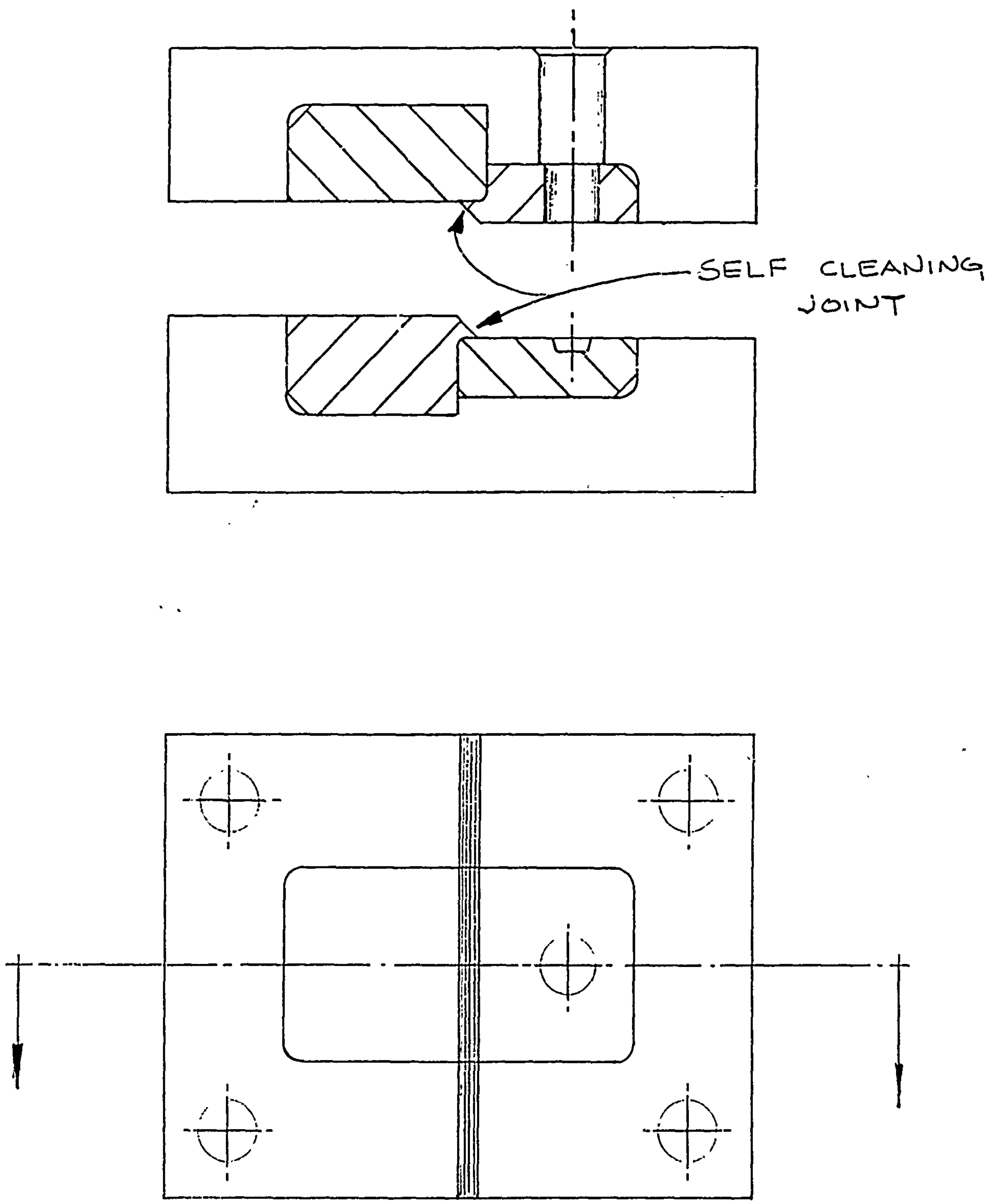


Figure 44 - Sketch illustrating Doehler's self-cleaning joint concept.

The upper half (die end) was made from high-density, pressed and sintered molybdenum. That portion of the shot sleeve liner was copper brazed to the high-density, pressed and sintered molybdenum cover-half impression insert forming an integral body. (The purpose of that construction was to avoid flash penetration and gap formation at the interface between the die and the shot sleeve liner.) The wall thickness of the lower portion of the molybdenum shot sleeve liner was reduced to 1/4" so that it could be slipped into the AISI H-13 shot sleeve. The lower, or plunger end, of the shot sleeve liner was conceived to be a replaceable element. It was fabricated from AISI H-21 steel;* and it, too, had a 1/4" wall thickness. The joint between the molybdenum and H-13 elements of the shot sleeve liner was approximately 6" below the parting plane of the die, well below the bottom of a normal biscuit. (That location was selected to avoid the possibility of forcing molten metal into the joint under high pressure.)

Four holes were drilled in the H-13 shot sleeve, parallel to its axis, to accept cartridge heaters. The shot sleeve could be removed or installed from below, guided by two pins attached to the "top" clamping plate (the lower plate, in this case). A split clamping ring supported the shot sleeve body, holding it rigidly in position. Design details at the lower end of the shot sleeve provided for holding the liner in place, for removal of the liner, for cartridge heater leads, for a vacuum channel just above the o-ring, and for thermal insulation between the heated portion of the shot sleeve and the short cooled portion, near the bottom of the sleeve, surrounding the o-ring. The upper end of the shot sleeve was also insulated from the stationary platen with Fibrefrax felt encased in sheet metal.

A water-cooled, beryllium-copper plunger (Berylco-10) was employed by the Research and Development Center; and a room temperature clearance of 0.002" to 0.003" between the plunger and the bore of the shot sleeve liner was adopted.

In order to have an assured method of operation, in the event that the vacuum lift system had failed to perform, the Research and Development Center explored a variation of the approach used by Doehler Jarvis. Instead of using asbestos cups, 0.010" low-carbon steel was formed into seam-welded cups. The Morton machine was adjusted to operate in the short-stroke mode; the cups were inserted by hand; and filling was accomplished by transferring the molten metal to an inclined trough or launder, which delivered it to the shot sleeve. The trough was then swung out of the way, the dies were closed, and the metal was injected. The method proved to be simple and effective, the can apparently having sufficient insulating value and a low enough heat capacity to avoid freezing in the shot sleeve. However, as illustrated by Figure 45, the crumpled can obstructed the gates of the die, resulting in very poor filling.

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*Nominal composition: 0.35% carbon, 0.3% manganese, 0.3% silicon, 3.35% chromium, 9.0% tungsten, 0.25% vanadium, balance iron.

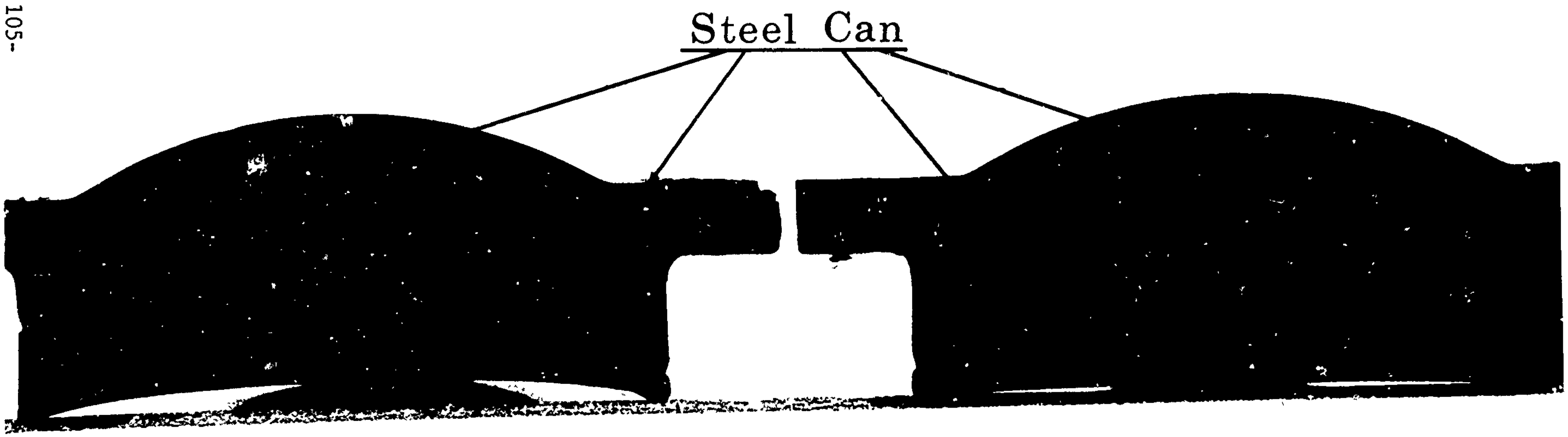

Figure 45 - Section through a biscuit produced during a hand ladling experiment on the Morton Vertacast Machine with the shot sleeve protected by a thin-walled steel can.

SECTION IV

DIE DESIGN

A total of six refractory metal dies were employed in the effort related to the project. Five of those dies were designed early in the course of the program, using the principles developed in the pioneering work of the Lamp Metals and Components Department for guidance. It is the design of those first five dies that will be considered here. The sixth die, which was designed later in the project for the pilot production of die cast hemispheres, reflected a refinement of the principles of die design based on the experience garnered during the course of the project. The design of that die will be discussed in Section VIII.

The five dies to be considered in this section are:

a. A test bar designed by the Research Center for interim service

b. A hemisphere die designed by Doehler Jarvis

c. A test bar designed by Doehler Jarvis

d. A materials evaluation die designed by the Lamp Metals and Components Department

e. A prime test bar die designed by the Research and Development Center.

1. TEST BAR DIE DESIGNED BY THE RESEARCH CENTER FOR INTERIM SERVICE

Although the Research and Development Center designed and built a rather sophisticated test bar die for use on their Morton Vertacast Machine, the many time delays which were encountered in that activity endangered the Research Center's primary objective. To achieve that objective, i.e., the determination of the effect of casting parameters on the quality of ferrous die castings and on the mechanical properties of die cast ferrous alloys, the Research Center was forced to seek a means of producing die castings prior to the completion of their prime test bar die. Through the cooperation of the Dort Metallurgical Company, the Research Center was offered such an opportunity.

Dort had built a die for another project in which the cavities occupied only the lower half of the area available in the "A" and "B" plates. To accomodate the test bar cavities which the Research Center wished to introduce, it was only necessary to extend the existing

runner system. Figure 46 indicates the disposition of the six cavities which the Research Center had sunk into two sets of impression blocks. The six cavities were designed to simultaneously produce four impact specimens and two tensile bar specimens. The two impression blocks used by Dort for their proprietary project are represented as blanks.

To put the interim test bar die into service in the shortest possible time, the Research Center had to use materials that were immediately available. One set of impression blocks was fabricated from wrought molybdenum (GE-22); the other was fabricated from an experimental 80% molybdenum 20% tungsten alloy (80-20). Both of these materials had previously been supplied to Dort by the Lamp Metals and Components Department. The size of the impression blocks made it most expedient to permit the central runner to run along and stradle the interface between the two impression blocks in either die half. The tendency towards gap formation, under such circumstances, was noted earlier, and that phenomenon (the mechanism of which is described in Section III) proved most troublesome in the interim test bar die.

The performance of the interim test bar die and the other dies used in the early stages of the project is discussed in detail in Section V.

2. HEMISPHERE DIE DESIGNED BY DOEHLER JARVIS

The hemisphere die designed by Doehler Jarvis was probably the most complex die designed under this contract because of its intended mission. Not only was it intended to permit Doehler to evaluate design concepts, but it was also designed to permit Doehler to perform life testing on several die materials. The design finally evolved into a three-cavity die with easily replaceable elements. The die was to produce hollow hemisphere castings similar in configuration to the BLU/26 which was then being die cast in aluminum by Doehler Jarvis and others. The outer configuration of the ferrous hemispheres was intended to be identical to the BLU/26. The configuration of the mating surfaces of the ferrous castings was also to be identical to that of the BLU/26, but the circumferential groove, which is cast into the flutes of the BLU/26 to accept a locking ring, was intentionally omitted. If a ferrous die casting were to phase out the BLU/26, it would necessarily have the same loaded weight. With this in mind, Doehler designed their hemisphere die to produce castings with thinner walls than the BLU/26; but to accommodate the desired configuration of the mating surfaces, that thin wall had to blend into a heavier skirt near the parting line.

There was a possibility, too, that a ferrous die casting would require some internal grooving to equal the performance of the BLU/26. Recognizing that possibility, Doehler Jarvis made provision to cast vee grooves into the inner surfaces of the hemispheres. To accomplish that objective, Doehler added two raised vee-shaped polar ribs to the design of the ejector impression inserts.

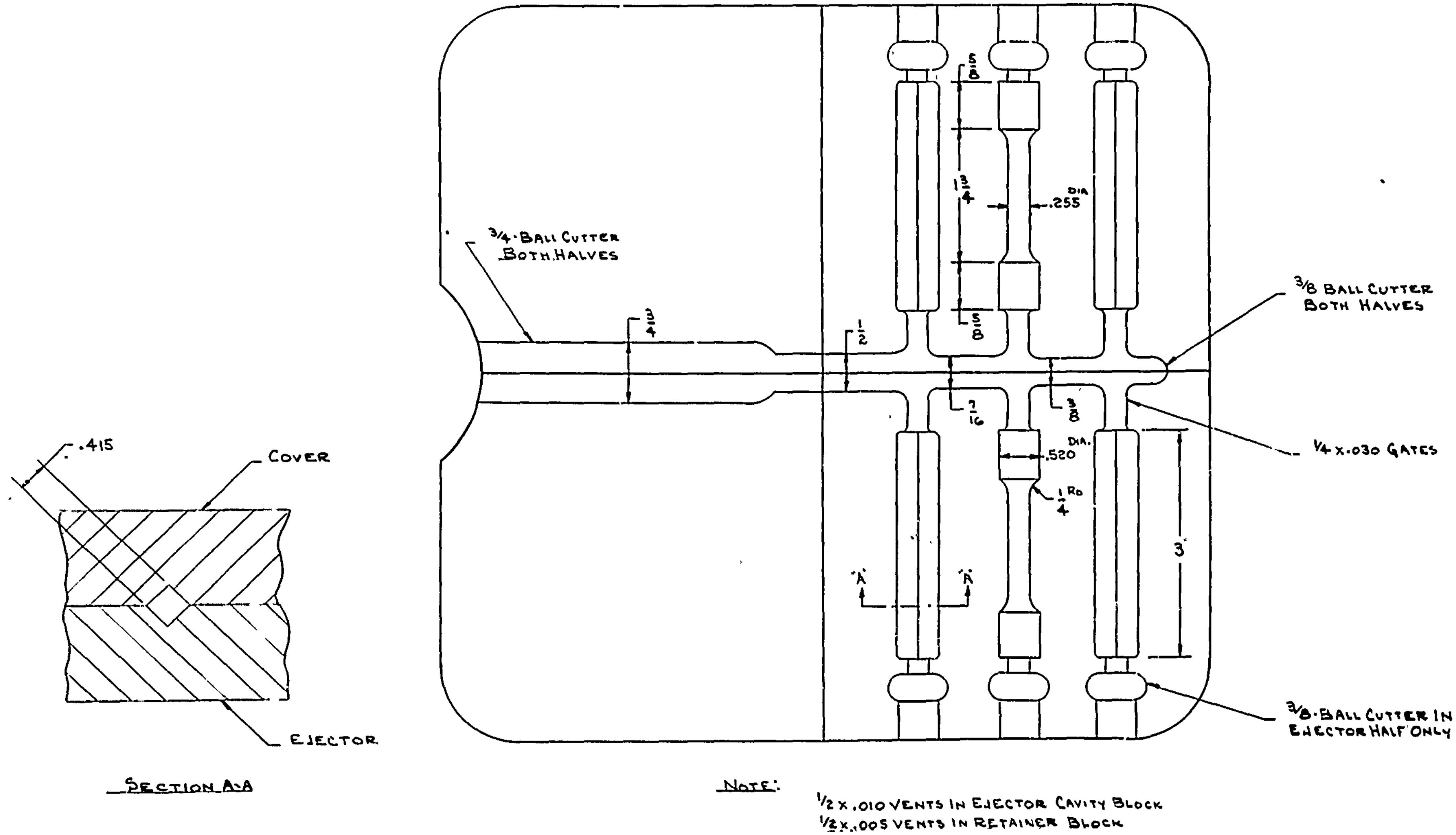

Figure 46 - Face view of the interim test bar die designed by the Research and Development Center (ejector half).

The hemisphere die was to consist of a cover and ejector half mounted in a die base containing a hydraulic ejector system. The die was designed for insertion in a 500 ton vertical die casting machine designed and built by the Doehler Jarvis Division of the National Lead Company.

The cover and ejector die halves were both to be made of refractory metals surrounded by steel. The outer portion, or holding die, was to be made of H-11 steel. It was to be rectangular with a rectangular cavity. One side of the holding die was to be made of a block of H-11 steel, doweled and screwed to the main block. By removing this block, the inserts could be quickly removed.

The gate block, designed to be made of high-density, pressed and sintered molybdenum, was to be held in place using a variation of the tongue-and-groove principle along two sides. Type 304 stainless steel blocks were to be used along one side of the gate block as expansion compensators between the steel and the refractory metal inserts.

Because the thermal expansion of steel is much greater than that of the refractory metals, the die designer is confronted with a problem. The A and B plates, which are typically made from steel, tend to expand more than the refractory metal inserts which they contain when the die is heated. This may result in misalignment of blocks from one die half to the other. Joints between blocks may become enlarged, permitting metal flash to penetrate them, damage the inserts, and impede casting removal. The following provisions were made by Doehler Jarvis to achieve tight joints:

a. A form of tongue-and-groove was used between the impression blocks and gate block to keep these joints tight. A rectangular molybdenum key was used between these blocks to prevent lateral movement. (Because the pressed and sintered tungsten-2% ThO_2 which was used for one ejector half impression block presented machining difficulties, breaking out from cutter contact and clamping pressure, a shouldered design, rather than a tongue-and-groove design, was used to hold down that block.)

b. The side of the impression blocks opposite the gate block was held down by a wrought molybdenum hex head bolt screwed into the steel holding block at a 45° angle. The concept was to tighten the bolts after the die had reached its operating temperature, thereby causing the impression blocks to press forward against the gate blocks. It was hoped that stresses created by the running conditions would not fracture the wrought molybdenum or the high-density, pressed and sintered molybdenum blocks in this area. (Screws were not used in

the pressed and sintered tungsten-2% ThO_2 impression block, because it was felt that the screw head load would cause damage to that material; therefore, a block of 304 stainless steel was keyed into the pressed and sintered tungsten-2% ThO_2 and the screws were set into the stainless steel.)

c. Another design concept for achieving and maintaining tight joints between refractory metal blocks in a steel plate was to insert a third material with a coefficient of thermal expansion much higher than that of either the refractory metal or the steel. Austenitic stainless steels satisfy this requirement, and Doehler Jarvis selected 304 stainless steel to evaluate the technique.

(One important general design principle when working with refractory metals is to avoid internal threads. It can be seen from the foregoing that Doehler Jarvis observed that precaution conscientiously.)

Although the inexpensive approach to the construction of a hemisphere die would have been to use inserts in the ejector half impression blocks to form the ID's of the hemispheres, there was some concern about the efficiency of heat removal using such a design. To resolve that uncertainty, Doehler decided to make the core of one impression an integral part of the ejector half impression block so that its performance could be compared with that of the other two impression blocks. The design also provided for the evaluation of three alternate methods of retaining inserts in the two remaining impressions.

Therefore, the design generated by Doehler specified that the ejector impression block for Impression 1 be fabricated from a single, solid block of high-density, pressed and sintered molybdenum. For Impression 2, the ejector impression block was designed to accommodate shouldered inserts; and for Impression 3, the ejector impression block was designed to accommodate inserts held in place either by molybdenum dowels or molybdenum cap screws.

AISI A-2 steel, which retains useful strength up to 1550°F, was specified for the doweling (leader pins) between the die halves and for the four surface pins.

Initially, it was not known whether the die would require heating, or cooling, or both. Some refractory metals have ductile-to-brittle transition temperatures far above room temperature. For ferrous die casting dies made from those specific materials, elevated operating temperatures were believed to be essential. Elevated operating temperatures were also believed to be generally helpful in prolonging the

resistance of dies to thermal fatigue and in producing castings with superior surfaces. On the other hand, high die temperatures were suspected to increase the risk of soldering and were known to cause disadvantageous reductions in the strength and hardness of die materials. In the face of this dilemma, Doehler made provision to either heat or cool the die or to alternately heat and cool the die as required.

For initial heat-up and supplemental heating as required, the die design provided for six 240 volt 3700 watt cartridge heaters in the cover die, and five 4900 watt cartridge heaters in the ejector die, for a total rated heating capacity of 46.7 kilowatts. Chromalox heaters with Type A hermetic seals were specified.*

The design provided for cooling each impression block with one single-return type 1/4" water line. The gate blocks were to be cooled by 1/4" in and out water lines.

To monitor the die temperature, the design incorporated metal-clad thermocouples at seven locations. Megopak thermocouples were specified.**

It was also considered desirable to insulate the die from the rest of the machine. Although it was recognized that minimizing the heat loss to the die casting machine might be important if the heat introduced by the casting operation were insufficient to maintain the die at the desired temperature, a more compelling motive was to limit the temperature rise of the die casting machine. To avoid the complications related to the thermal expansion of the platens, tie bars, and machine frame, and to avoid a decrease in the strength and rigidity of those members related to an increase in temperature, the design provided for insulation between the cover die and the machine plate (stationary platen) and between the ejector die and the die base legs. (Later, it was discovered that commercially available insulating materials did not have sufficient compressive strength to withstand the high unit loads under the die base legs.) Insulation was also provided to protect the seals of the hydraulic ejection mechanism from convection and radiation from the die directly beneath it on the vertical machine.

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*A product of Chromalox, Incorporated, a subsidiary of the Edwin L. Wiegand Company, Murfreesboro, Tennessee.

**Spring-loaded, bayonet type thermocouples manufactured by Honeywell, Incorporated, Minneapolis, Minnesota.

Ejection was designed to be supplied by four 5/16" molybdenum ejector pins per impression. In Impression 1, the ejector pins were located in the overflows. In Impressions 2 and 3, the ejector pins were designed to push against the mating surfaces of the hemispheres (i.e., overhanging type pins).

The final drawings for the three-cavity hemisphere die, designed by Doehler Jarvis to evaluate die materials and design concepts, are duplicated in Figures 47 through 51. The notes that have been assigned Arabic numerals in Figures 47 through 51 are preserved in Table XIV.

The die that materialized from Doehler Jarvis' design for a three-cavity hemisphere die is illustrated by Figures 52 through 54. It weighed 2700 pounds.

3. TEST BAR DIE DESIGNED BY DOEHLER JARVIS

In addition to the three-cavity hemisphere die which they designed, Doehler Jarvis also designed and built a test bar die. The purpose of that die, which was relatively simple compared to the hemisphere die, was to enable Doehler to begin their investigation of operating parameters, die design concepts, and injection system performance before the hemisphere die was ready for service and during those periods when the hemisphere die would, of necessity, be off the die casting machine.

Doehler's test bar die, which is illustrated by Figures 55 through 57, was a two-cavity die. One cavity was in the form of a tensile bar; the other was in the form of an impact bar. In both the cover and ejector dies, both cavities and the entire gate and runner system were sunk into a single block of high-density, pressed and sintered molybdenum, thus eliminating the need for the complex mechanisms incorporated in the hemisphere die for holding the several refractory metal blocks together. Provision still had to be made, however, to maintain registry across the parting line. This was accomplished by locating two mutually perpendicular keys in the parting plane between the B plate and the ejector impression block and a single key parallel to the cavities in the parting plane between the A plate and the cover impression block. The centerlines of each of these keys intersected the centerline of the shot sleeve, establishing that centerline as the neutral axis about which relative expansion could occur.

Doehler chose not to locate the impression blocks for the test bar die in closed pockets in the A and B plates but in open channels machined into the plates from one edge. The impression blocks were designed to

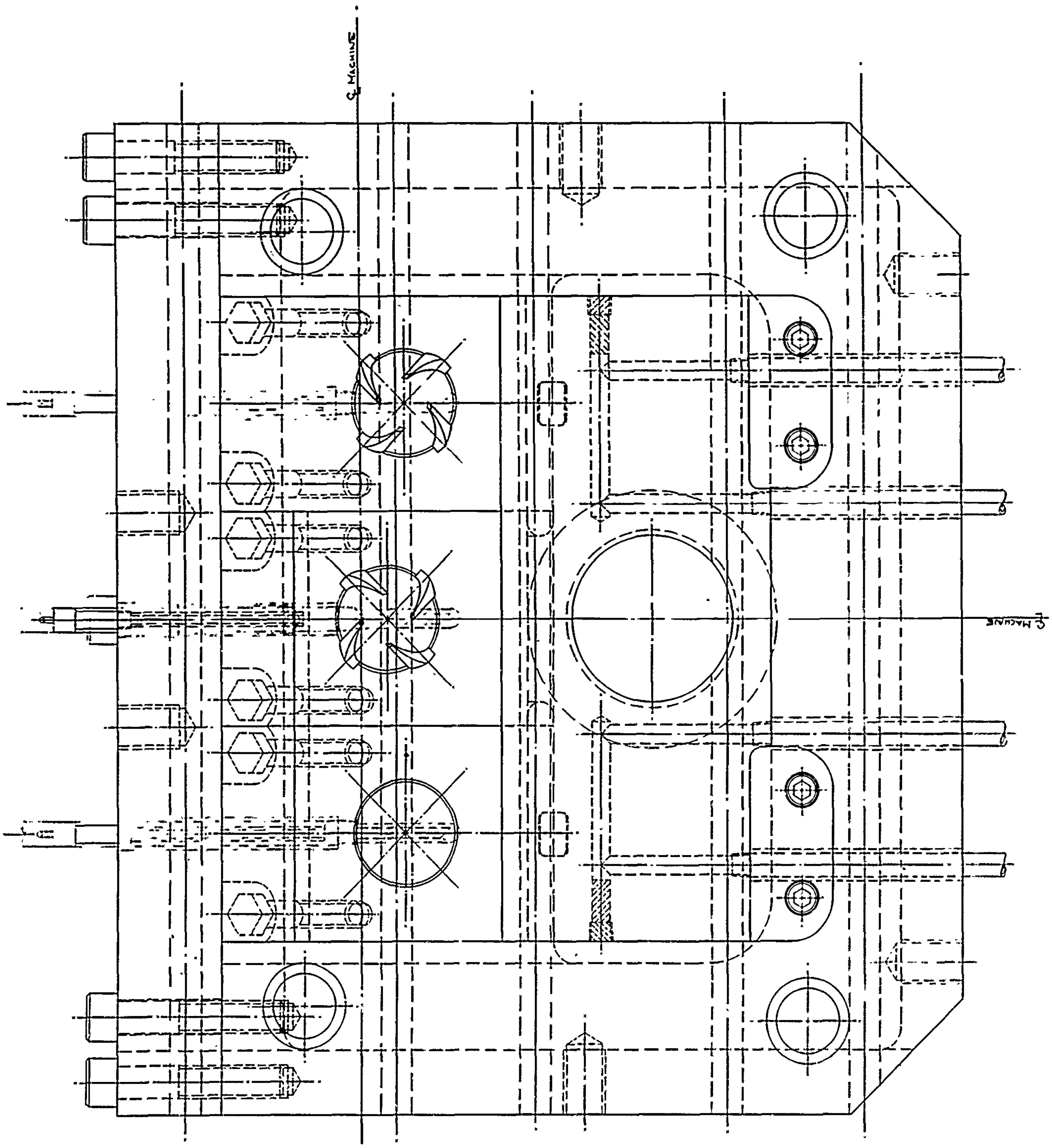


Figure 47: Face view of the cover half of the three-cavity hemisphere die designed by Doehler Jarvis.

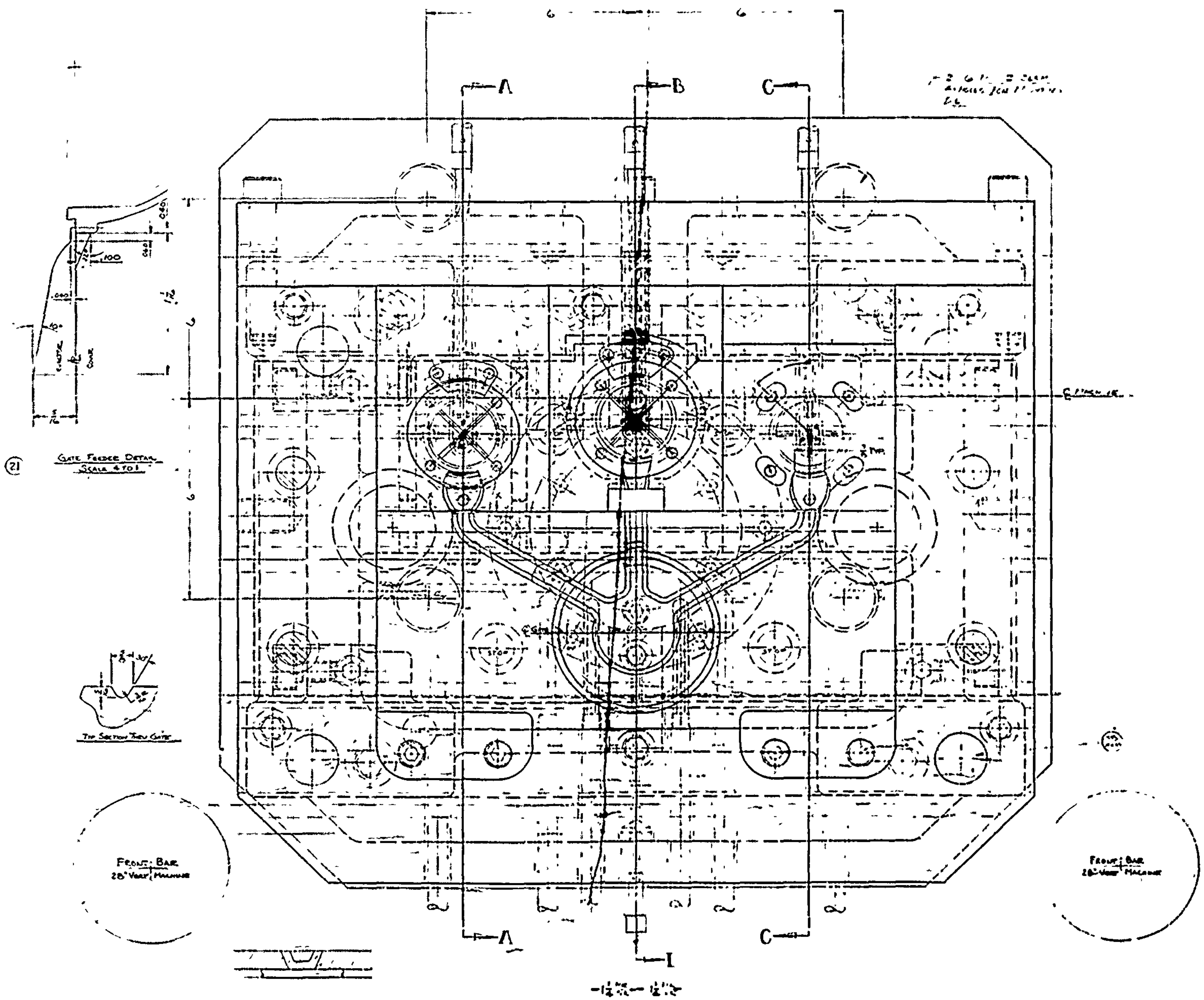


Figure 48: Face view of the ejector half of the three-cavity hemisphere die designed by Doehler Jarvis.

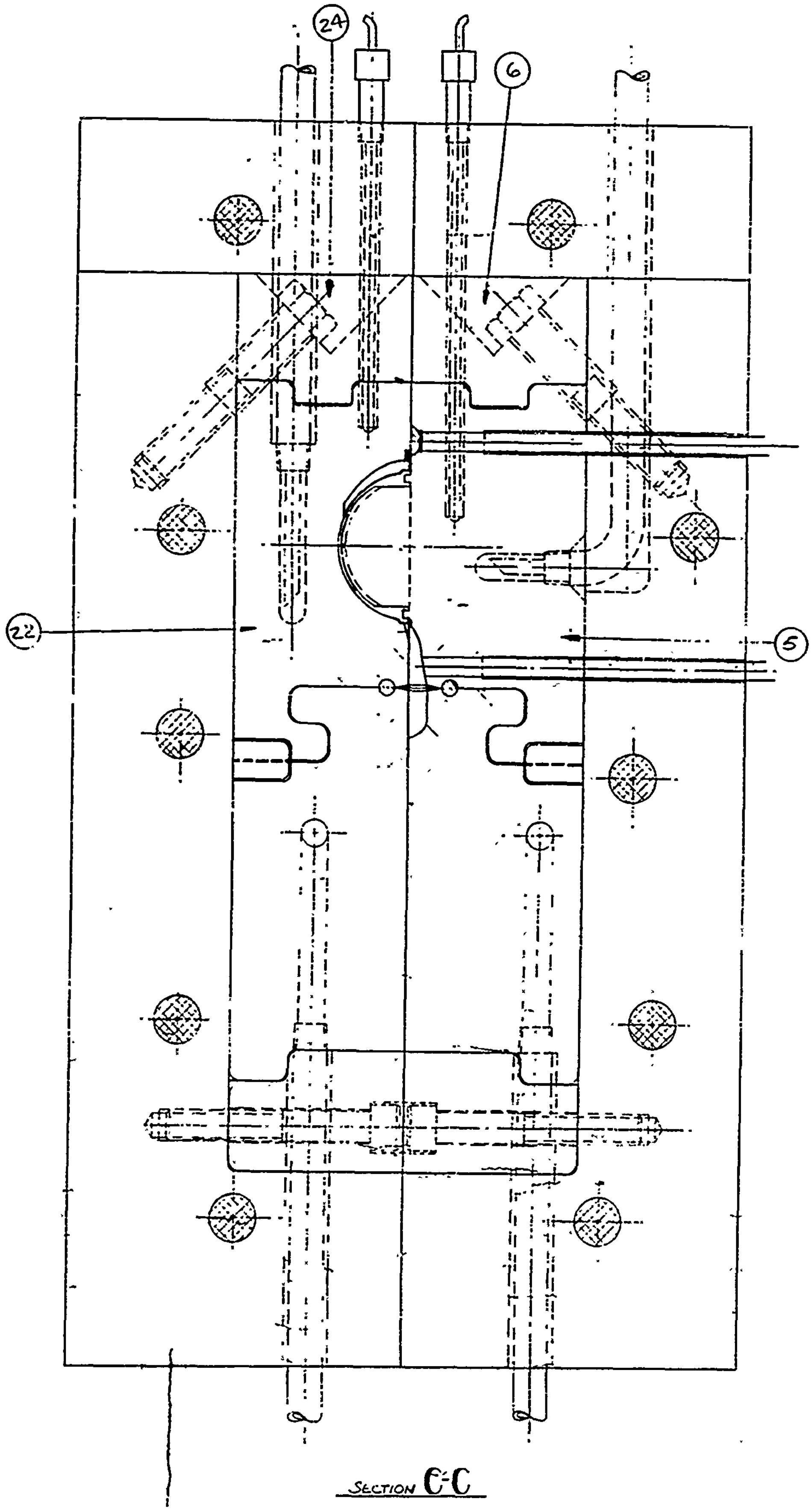


Figure 49: Sectional view of Impression 1 of the three-cavity hemisphere die designed by Doehler Jarvis.

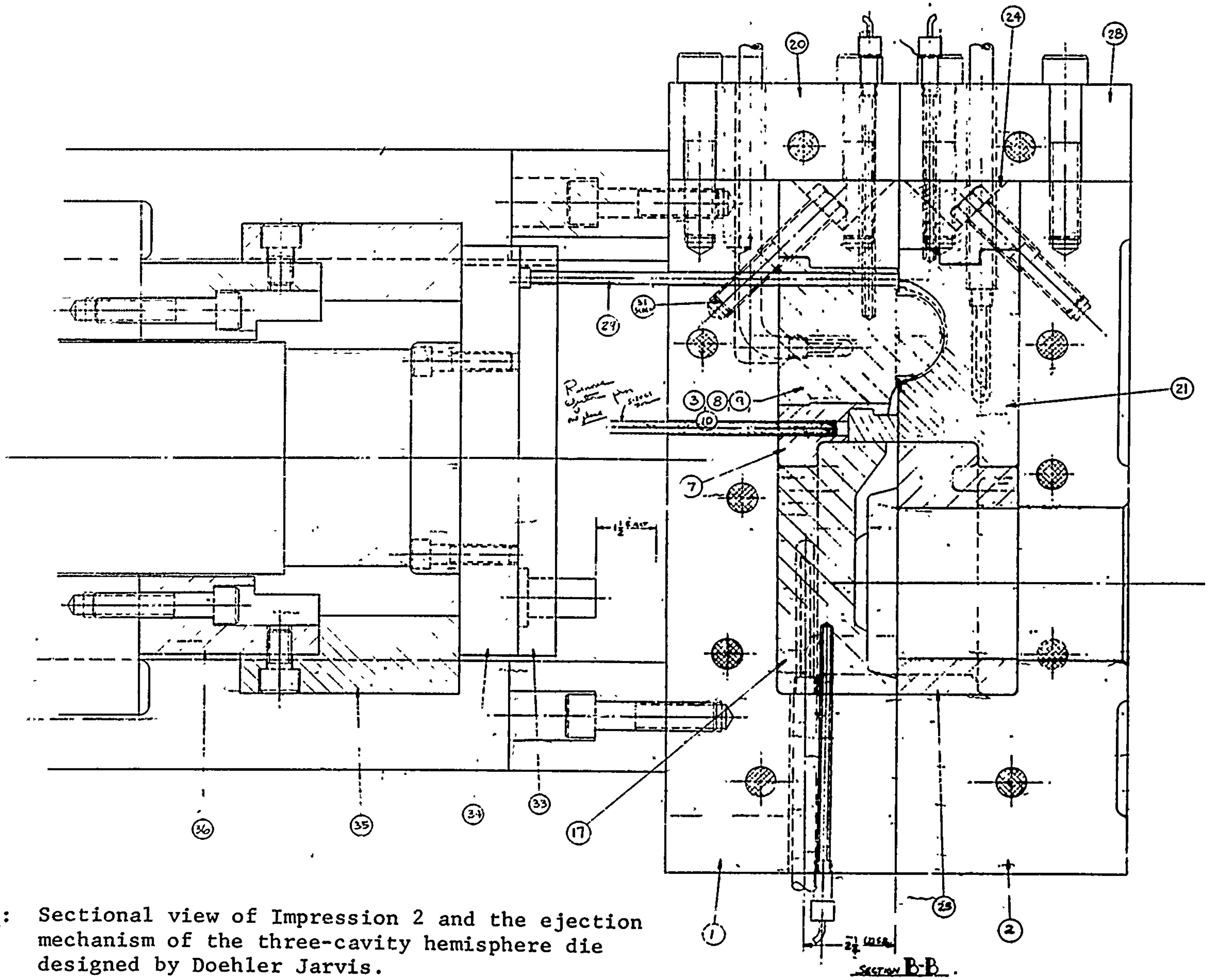
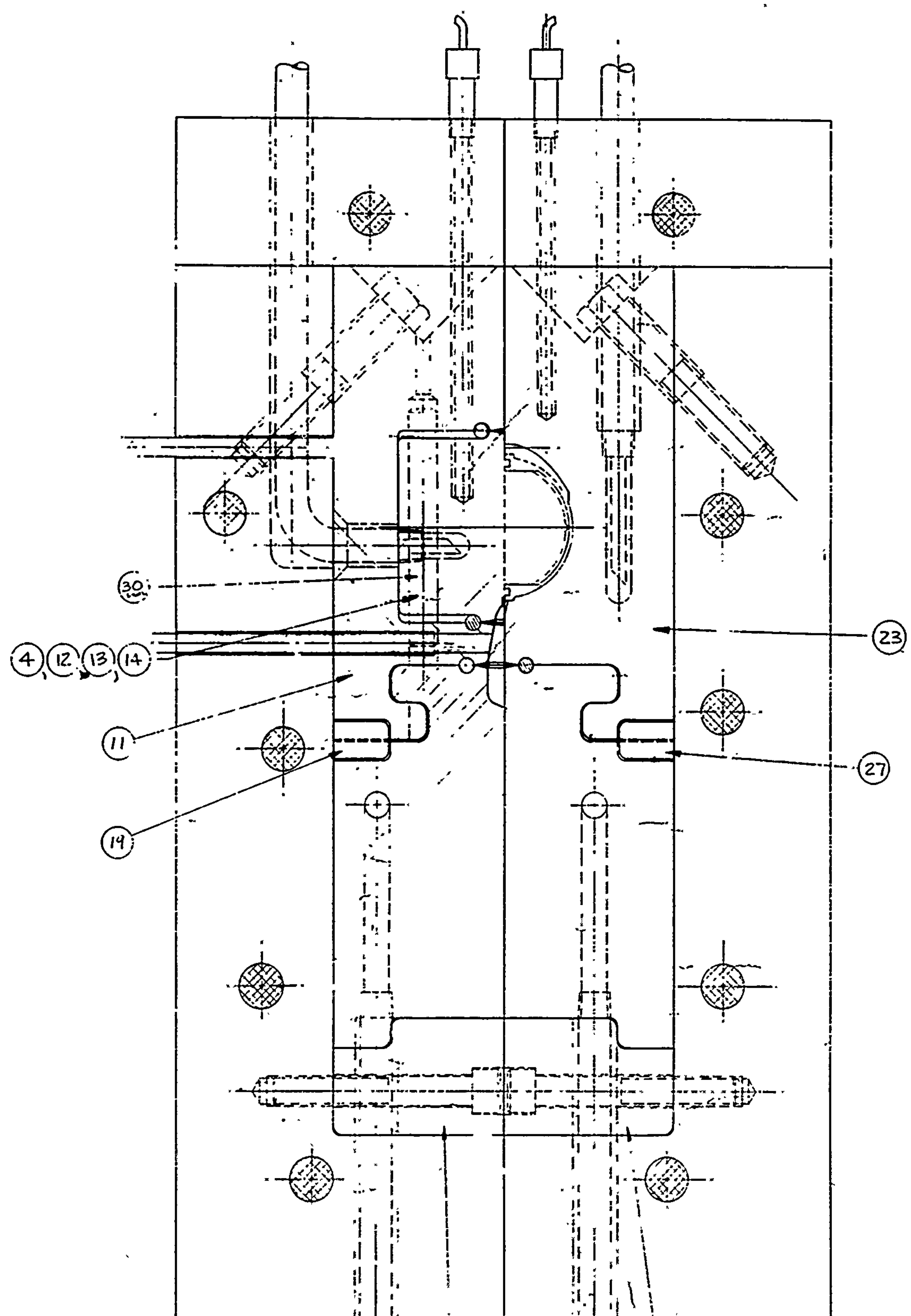


Figure 50: Sectional view of Impression 2 and the ejection mechanism of the three-cavity hemisphere die designed by Doehler Jarvis.



SECTION A-A

Figure 51: Sectional view of Impression 3 of the three-cavity hemisphere die designed by Doehler Jarvis.

Table XIV--Notes for the Drawings Reproduced in Figures 47 Through 51

Number of Note	Description	Material
1	"B" plate	"H" steel
2	"A" plate	"H" steel
3	Ejector impression insert	P&S tungsten - 2% thoria
4	Ejector impression insert	Wrought TZM
5	Ejector impression block	High-density, P&S molybdenum
6	Compensating block	304 stainless steel
7	Ejector insert retainer block	High-density, P&S molybdenum
8	Ejector impression insert	High-density, P&S molybdenum
9	Ejector impression insert	P&S tungsten
10	Ejector impression insert	Wrought molybdenum
11	Ejector insert retainer block	High-density, P&S molybdenum
12	Ejector impression insert	Wrought molybdenum
13	Ejector impression insert	Copper-infiltrated tungsten
14	Ejector impression insert	Anviloy 1150
17	Ejector gate runner block	High-density, P&S molybdenum
18	Ejector clamping block	304 stainless steel
19	Key	Wrought molybdenum
20	Ejector seal block	"H" steel
21	"A" style cover impression block	P&S tungsten - 2% thoria
22	Cover impression block (plain impression)	High-density, P&S molybdenum
23	"B" style cover impression block	Wrought molybdenum
24	Compensating block	304 stainless steel
25	Cover gate block	High-density, P&S molybdenum
26	Cover clamping block	304 stainless steel
27	Key	Wrought molybdenum
28	Cover seal block	"H" steel
29	Ejector pins	Wrought molybdenum/Hotwork steel
30	Insert retaining dowel	Wrought molybdenum
31	Hex head cap screw	Wrought molybdenum
33	Ejector retainer plate	
34	Ejector plate	

Figure 52 - Photograph of Doehler Jarvis' complete three-cavity hemisphere die, including the cover and ejector halves, the mold base, the ejection mechanism, and five interchangeable ejector impression inserts.

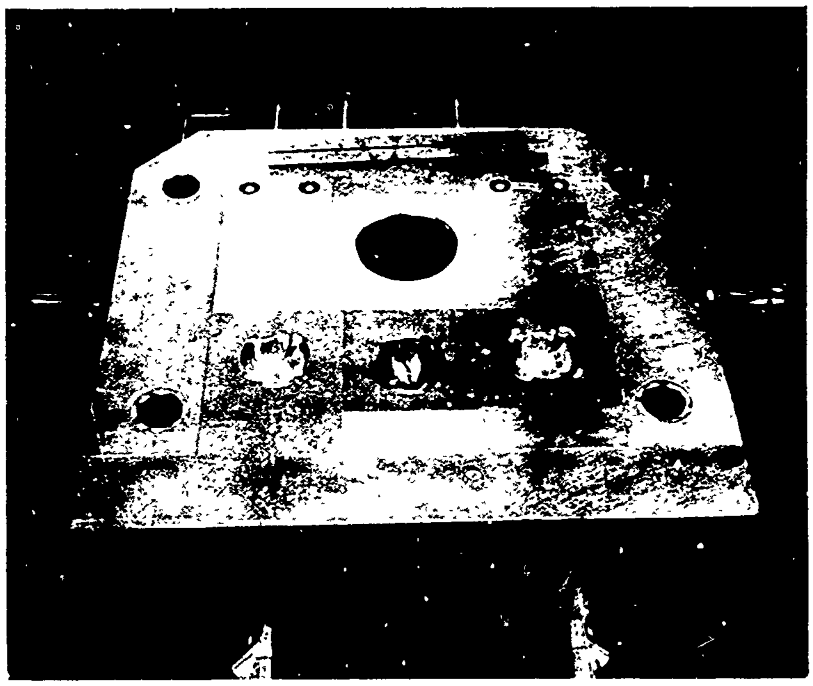


Figure 53 - Photograph of the cover half of Doehler's hemisphere die.

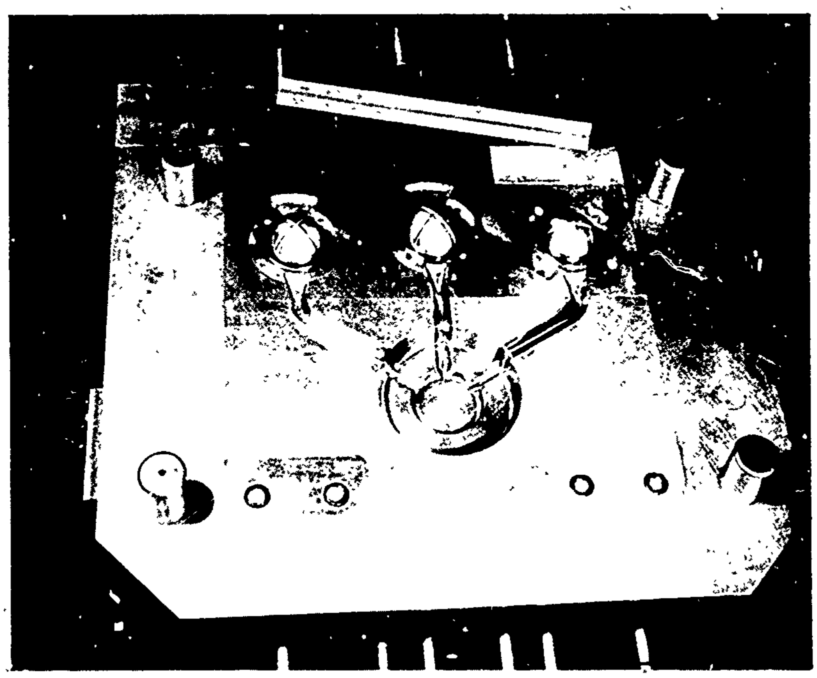


Figure 54 - Photograph of the ejector half of Doehler's hemisphere die.

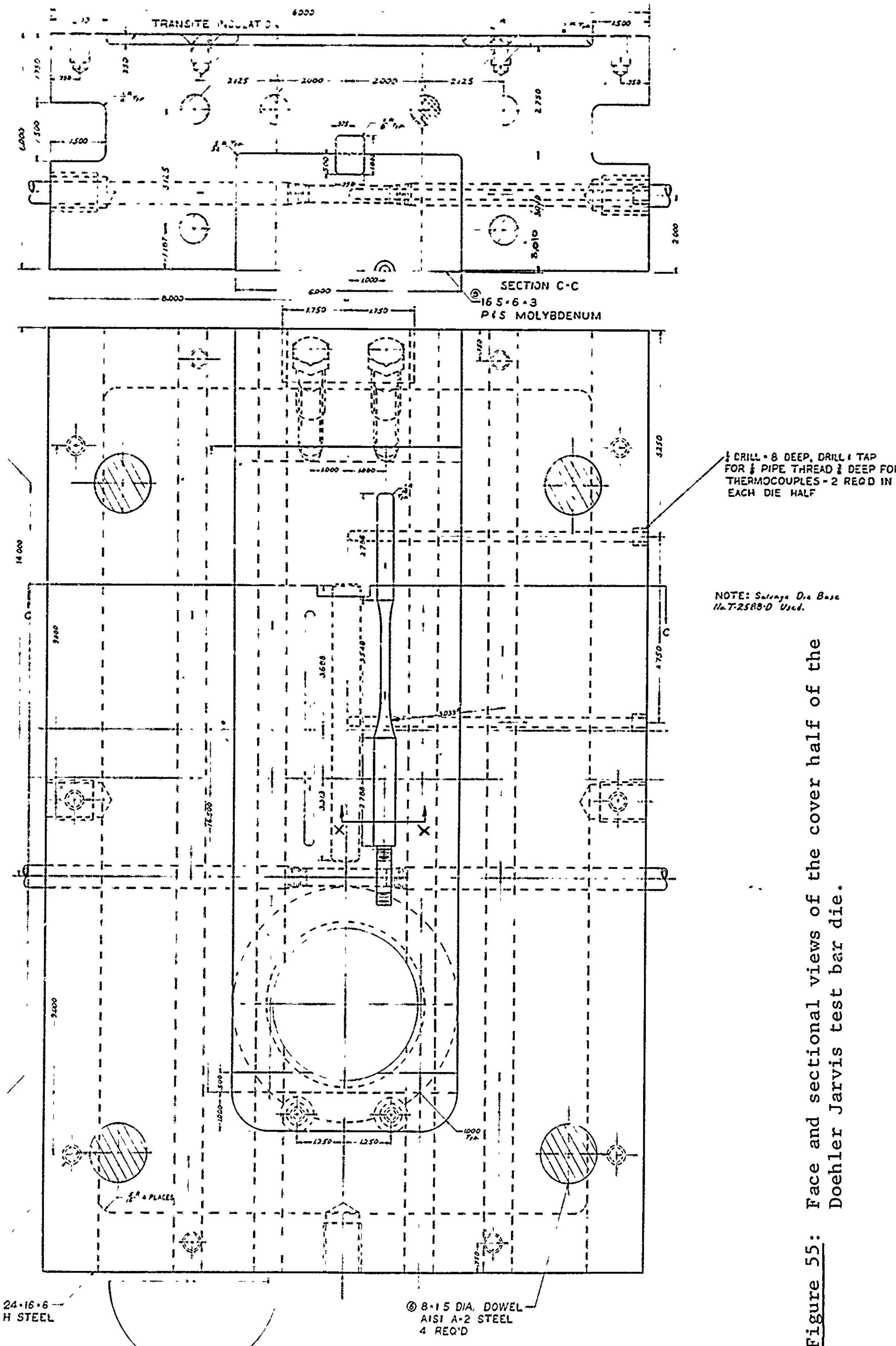


Figure 55: Face and sectional views of the cover half of the Doehler Jarvis test bar die.

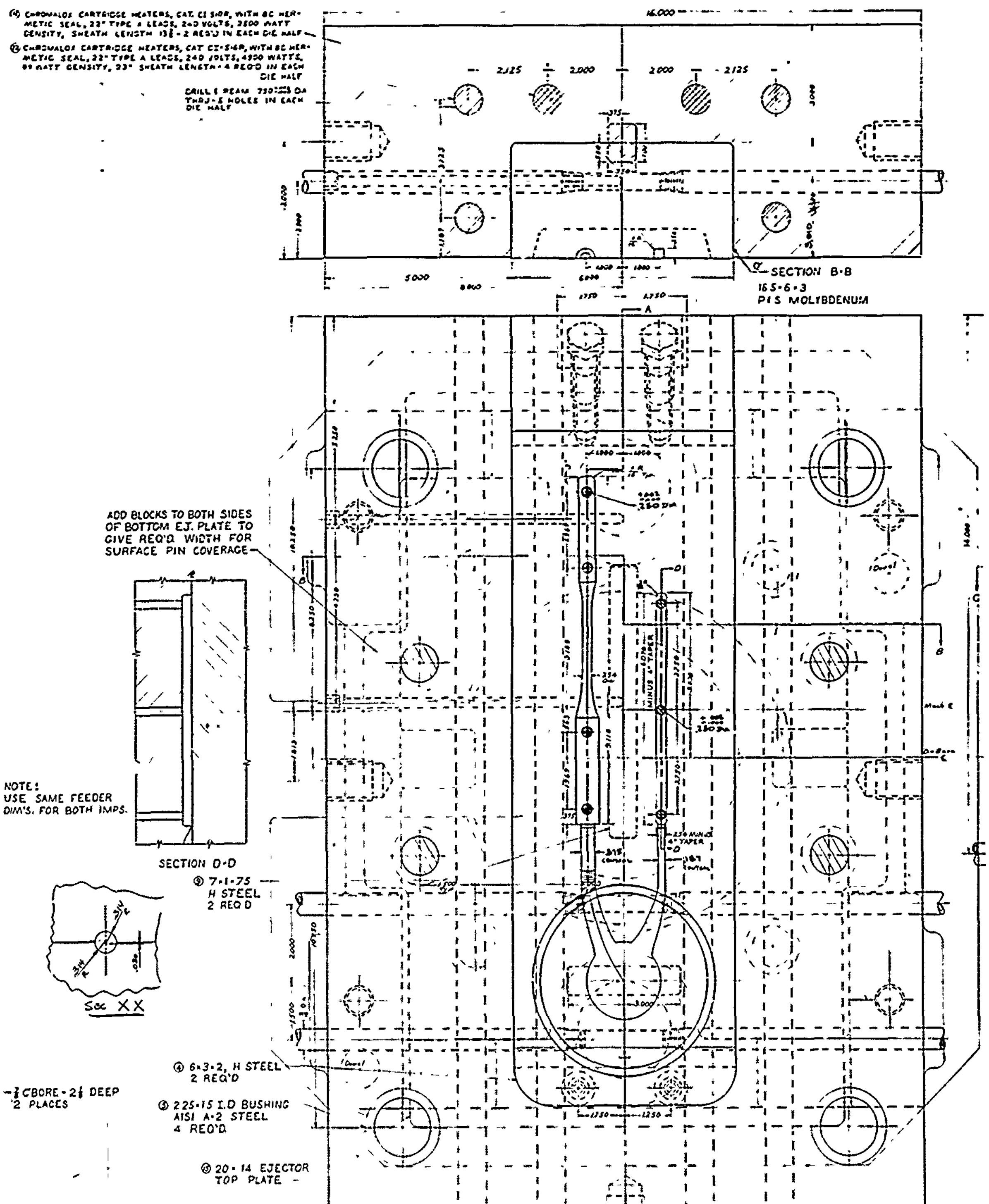


Figure 56: Face and sectional views of the ejector half of the Doehler Jarvis test bar die.

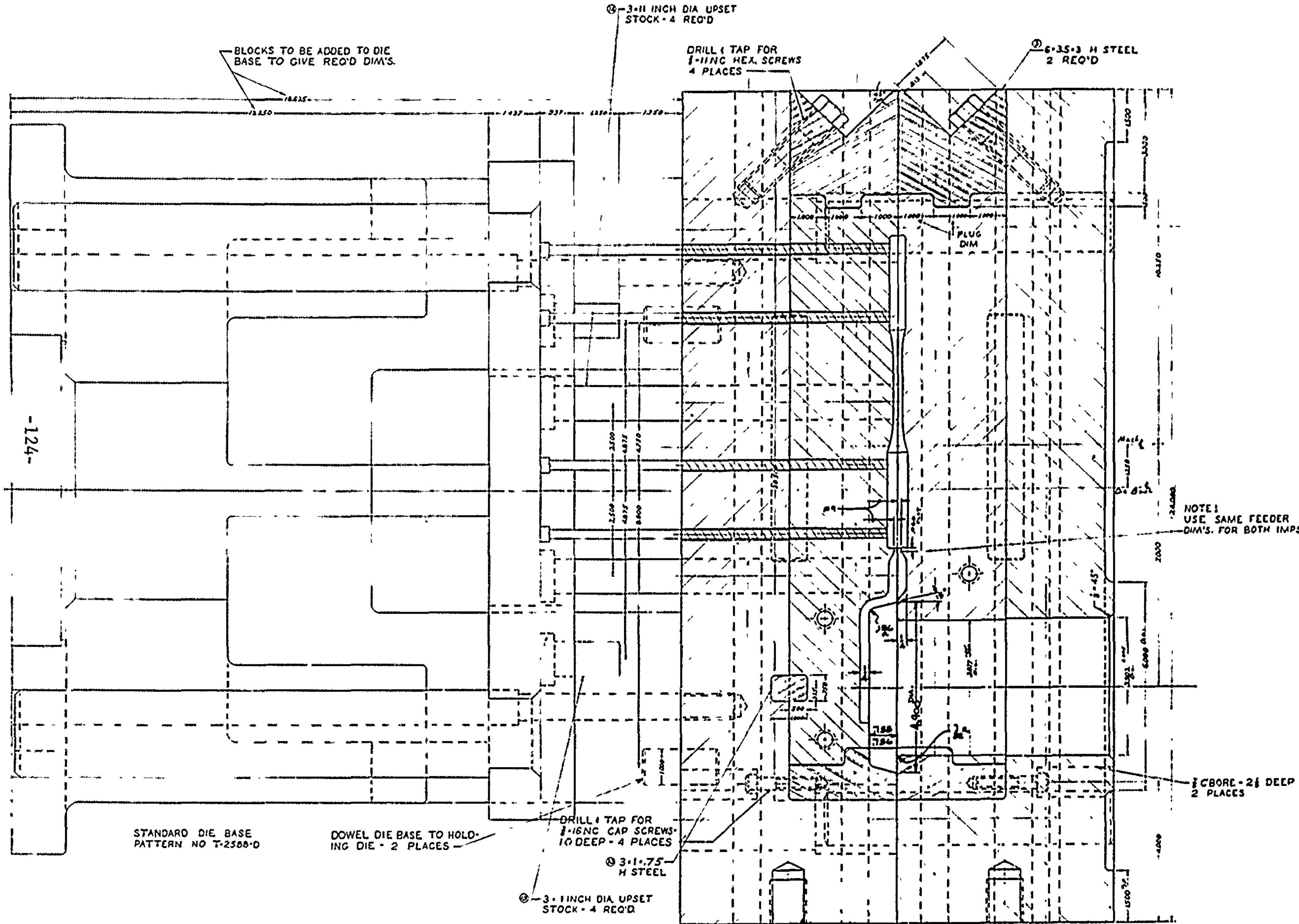


Figure 57: Sectional view of the Doehler Jarvis test bar die.

be held into those channels by steel blocks. The steel blocks employed at the open end were keyed to the impression blocks and fastened to the A and B plates by socket head cap screws, countersunk into the steel blocks at an angle of 45° to the parting line. The steel blocks at the closed end of the channels rested on shoulders machined into the impression block and were attached to the A and B plates by socket head cap screws countersunk into the back of those plates.

Provisions for heating, cooling, and insulating the die were quite similar to those planned for the three-cavity hemisphere die.

The dimensions and configuration of the feeders (ingates) reflected good aluminum die casting practice. As will be noted in Section V, ferrous die casting requires a different approach.

4. MATERIALS EVALUATION DIE DESIGNED BY THE LAMP METALS AND COMPONENTS DEPARTMENT

One of the very important aspects of the project was the evaluation of potential die materials for ferrous die casting. That evaluation was originally planned to be performed in the three-cavity hemisphere die designed and built by Doehler Jarvis; and indeed, some valuable information concerning the performance of die materials was generated by Doehler. It soon became obvious, however, that Doehler would be unable to complete the required evaluation within the time remaining with the available funds. Factors that contributed to that situation were the unexpectedly high cost of construction of the relatively complex three-cavity die, unexpected delays in the construction of that die, failure of that die to provide continuous, trouble-free operation, the high cost of handmade insulating cups, the restrictions imposed on the production of castings by the cup-making operation and the melting facility, and the unexpectedly low casting rate attained.

Fortunately, the Lamp Metals and Components Department had independently undertaken an internally funded materials evaluation program that duplicated that contemplated by Contract F33615-68-C-1190 but was even more comprehensive in scope. A six-cavity die had been designed for that internally funded program, and the construction of that die was complete by the time it became obvious that an alternate solution was required to the problem of performing the required die materials evaluation. The design of that die is illustrated by Figure 58.

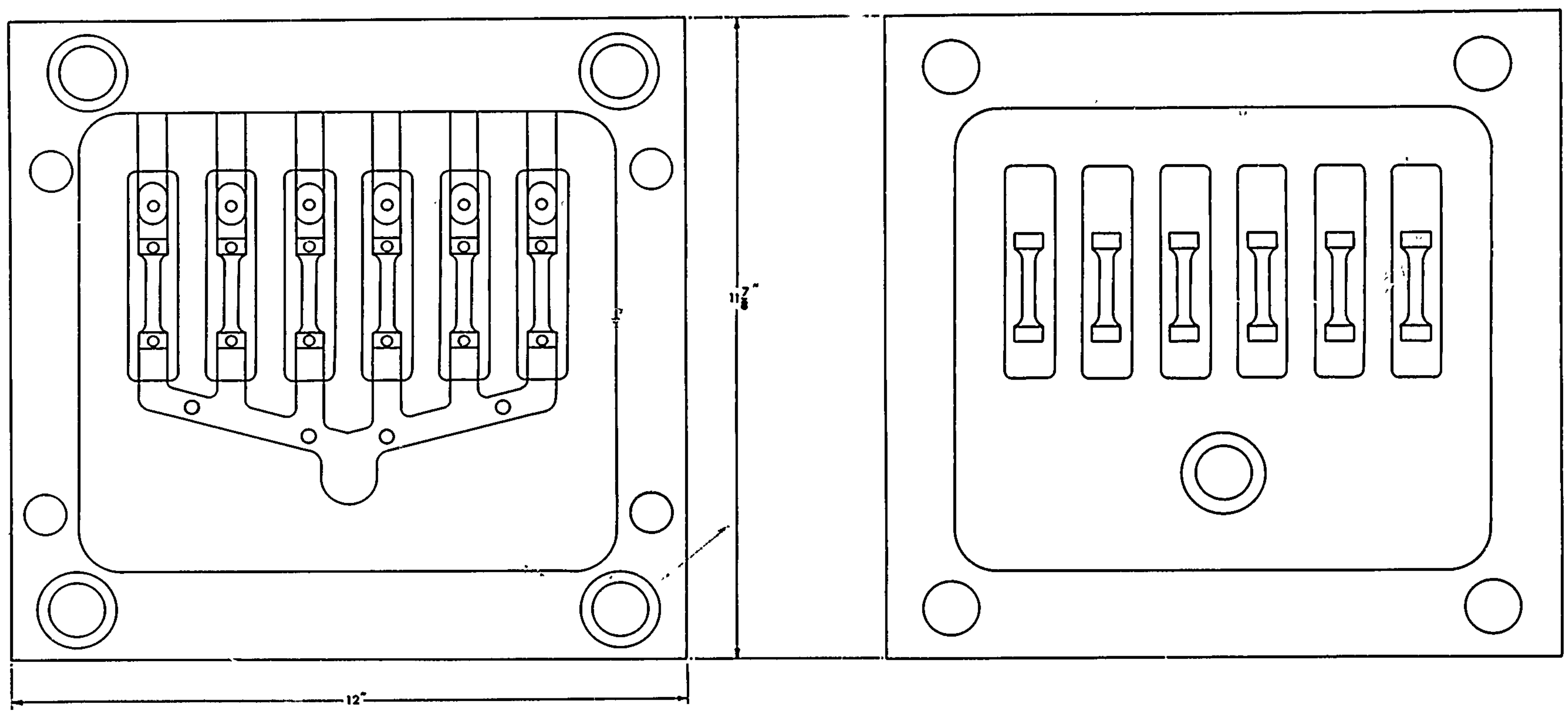


Figure 58: Face view of the cover and ejector halves of the Lamp Metals and Components Department materials evaluation die.

The challenge was to design a die that would provide multiple opportunities for the independent testing of a wide range of materials. Economic considerations dictated that the simultaneous testing of several materials be established as a goal, even though it was recognized that to attain optimum performance from any one die material, a unique combination of operating parameters might be required. Although the die materials would have to be classified into groups, it was nevertheless anticipated that each group might require a unique set of operating parameters. The significance of that consideration, with respect to die design, was that the die had to have the capability of stable operation over a wide range of die temperatures. Therefore, the materials evaluation die was designed to operate at temperatures up to 1000°F using cartridge heaters as a heat source. To reduce the heat loss to the die casting machine and the concomitant temperature rise in the platens, insulation, in the form of alternate layers of Transite* and stainless steel sheet, was specified between the stationary platen and the top clamping plate and between the movable platen and the bottom plate of the mold base.

To achieve the goal of providing multiple opportunities for independent testing, only one cavity was designed to be sunk into each insert. To reveal the response of the inserts to several distinctly different conditions of metal flow, the runner ends, in-gates, overflow gates, and vents were also sunk into the inserts. The inserts were designed to be retained in the pockets of two insert retainer plates which could also be test materials. The six-cavity, materials evaluation die, therefore, presented the possibility of simultaneously evaluating 14 different materials. Quite conventionally, the sprue and runner system were sunk in the ejector half of the die only.

Because the die was being designed to evaluate a variety of materials, and because very great differences in die life were anticipated, provision was made to enable individual inserts to be quickly and easily removed and replaced. To remove an insert, it was only necessary to remove the clamps holding the insert retainer plates to the top clamping plate or the support plate and withdraw the insert retainer plate as a unit. It was unnecessary to remove the insert retainer plate from the opposite half of the die and unnecessary to remove either the A or B plates. Having removed the insert retainer plate, individual inserts could be slipped out for inspection and/or replacement at will.

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*A cement-bonded asbestos product, produced by Johns-Manville, New York, New York.

The inserts originally designed for the materials evaluation die incorporated tensile bar cavities, as indicated in Figure 59. The cavities were to be sunk by electrical discharge machining.

While the die with the original set of inserts was under construction, it was observed in another die that crack initiation usually occurred in small-radius depressions (e.g., grooves). Therefore, a second insert design was prepared, incorporating depressions of very large and near-zero radii. This insert is illustrated in Figure 60. It possessed the additional virtue of being a shape that could be readily machined with conventional tooling by semiskilled machinists.

The appearance of the finished die before putting it into service is illustrated by the photographs reproduced in Figures 61 and 62.

PRIME TEST BAR DIE DESIGNED BY THE RESEARCH AND DEVELOPMENT CENTER

With the concurrence of the Air Force Project Engineer, the Research and Development Center designed their test bar die to produce:

a. Tensile test samples - normally 1.0" gage length, 0.250" diameter gage section, and 0.500" diameter gripping section

b. Impact test samples - standard Charpy V-notch test specimens, which measure 10 mm x 10 mm x 55 mm and have a central 45° V-notch 2 mm deep with 0.25 mm tip radius.

Standard Charpy impact specimens are 0.394" square by 2.165" long, with a centrally located V-notch across one face. For good test results, it is important that the specimens be exact and the notch precisely centered. In die casting such specimens, it had to be recognized that the exact shrinkage allowance was unknown and that a suitable draft angle would be necessary. These limitations were acknowledged by designing a cavity .415" x .415" x 3.0", oriented so that the parting line would be the diagonal of the square section. This orientation permitted easy ejection without departing from the square shape. The oversize dimension was designed to leave enough stock to permit final grinding to the exact .394" square dimensions. The initially square cross section simplified clamping for grinding.

To insure that the V-notch would always be centrally located, a special fixture was designed to permit cutting the specimen ends and milling the notch simultaneously. It was anticipated that the special arbor would not be disassembled, thus assuring specimens of identical length, with the notch precisely centered, throughout the casting campaign.

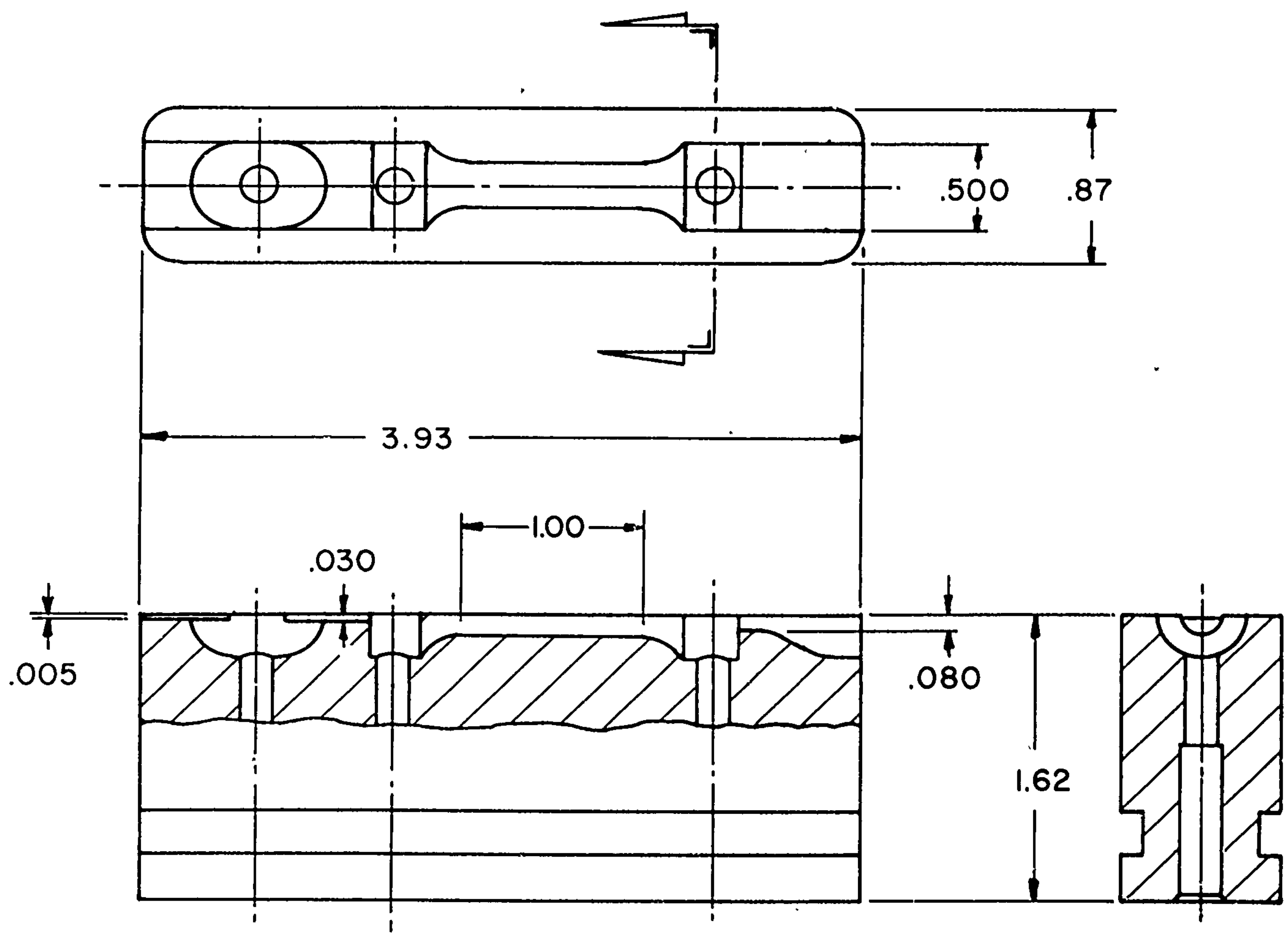


Figure 59: Original insert design for the materials evaluation die.

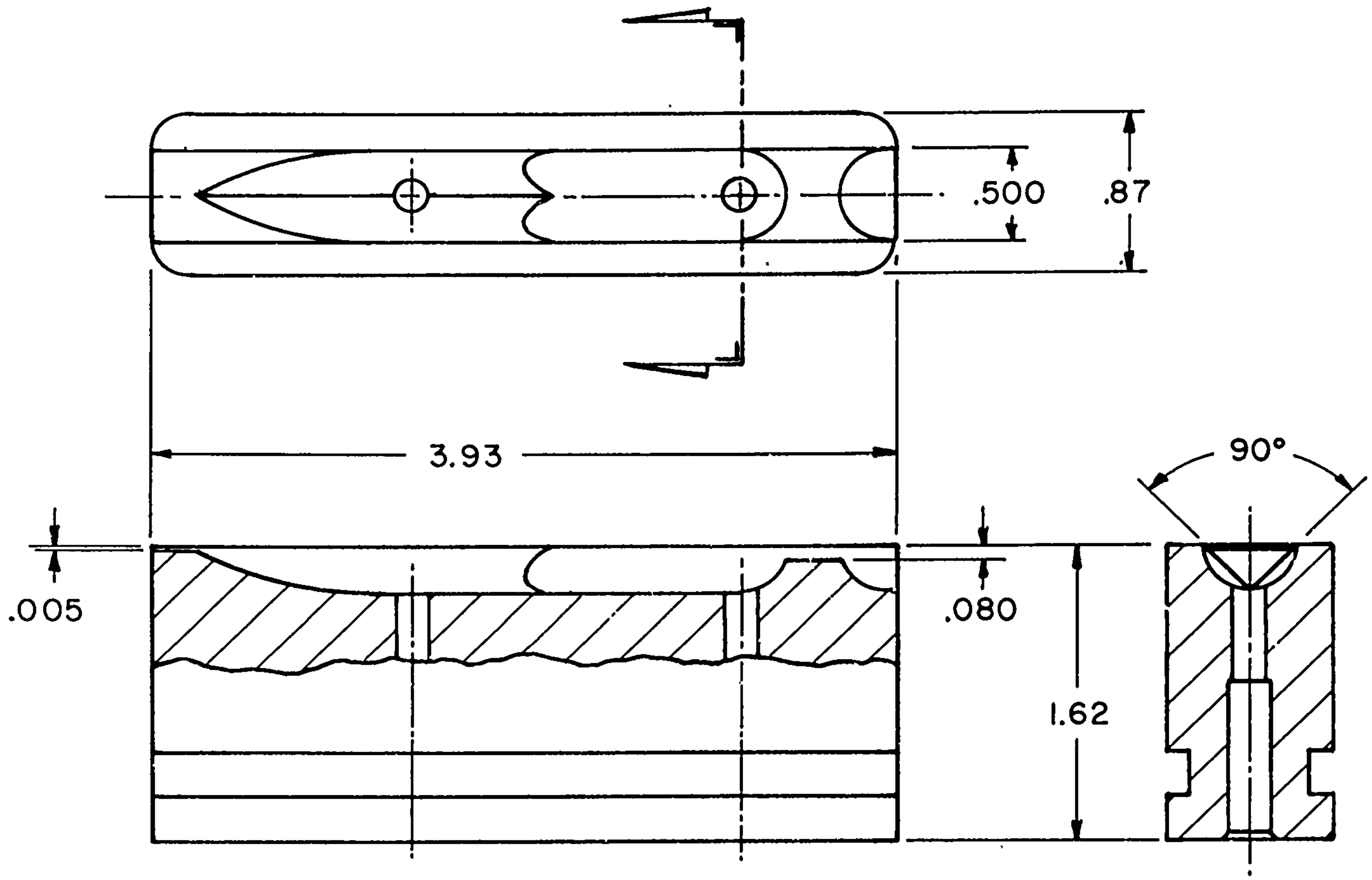


Figure 60: Replacement insert design for the materials evaluation die.

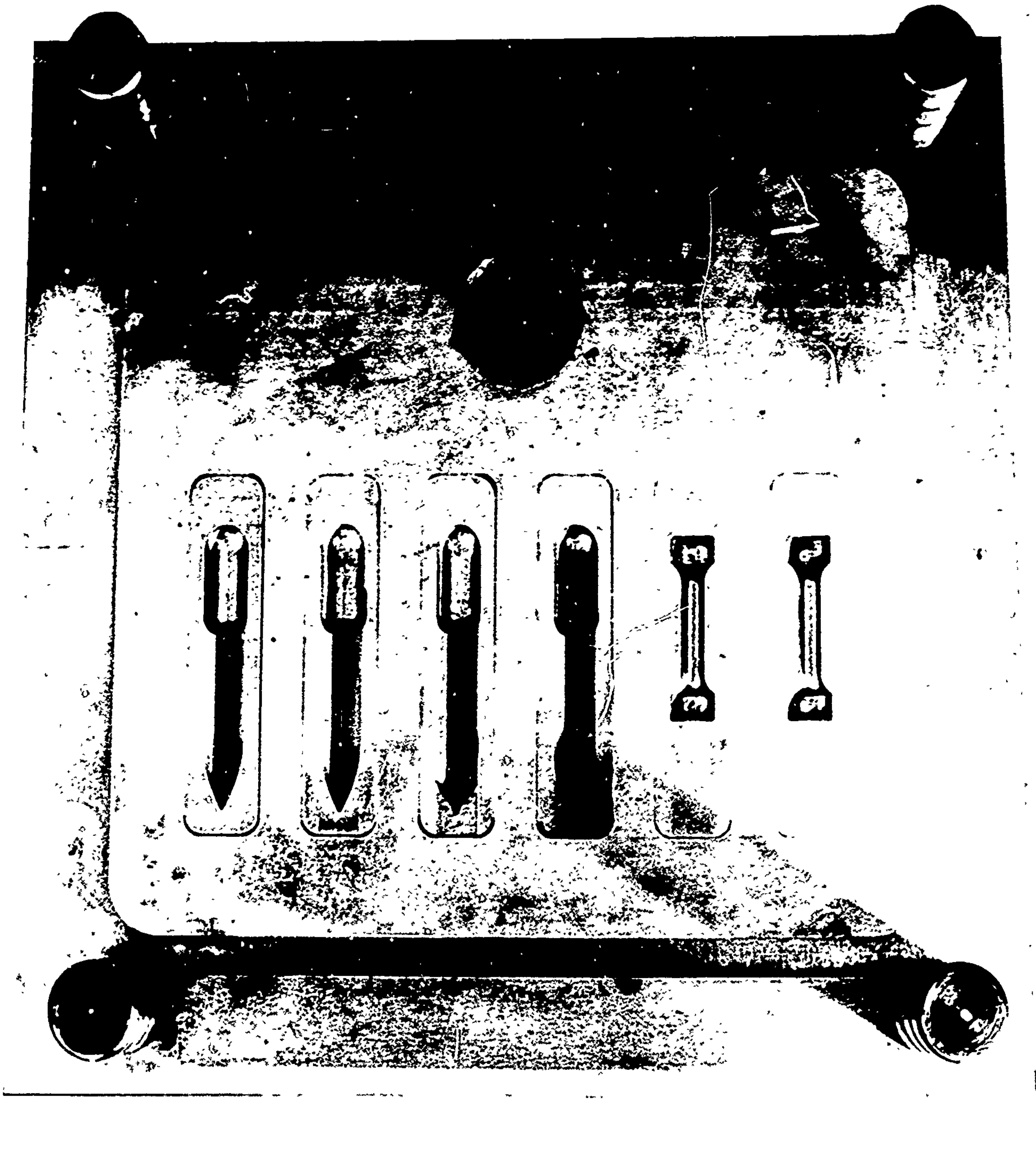


Figure 61 - Photograph of the cover half of the materials evaluation die designed by the Lamp Metals and Components Department--before being put into service.

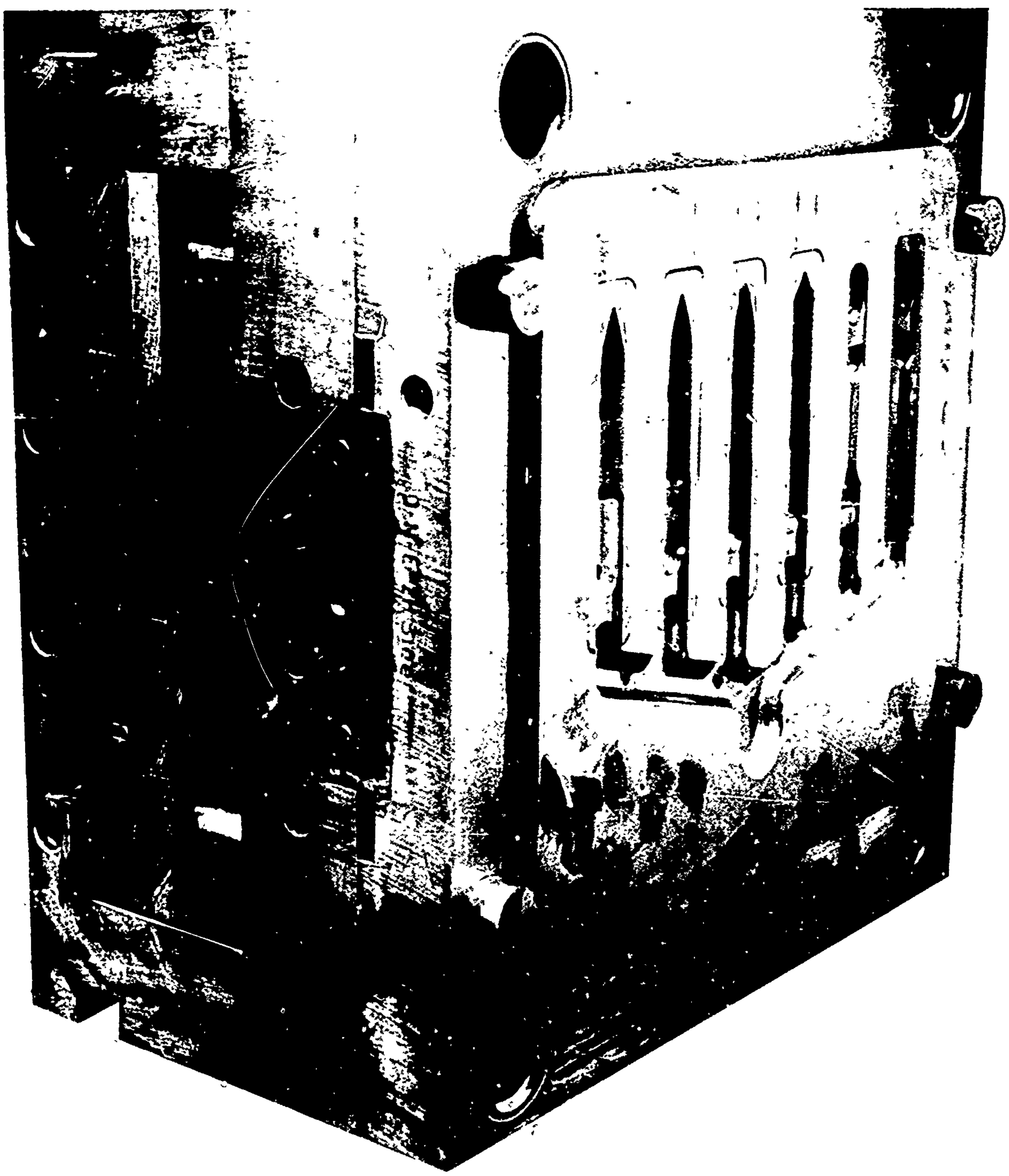


Figure 62 - Photograph of the ejector half of the materials evaluation die designed by the Lamp Metals and Components Department--before being put into service.

The die design also made provision for producing small 1/8" thick plates. The 1/8" thickness was selected to approximate the wall thickness of the hemispheres which were eventually to be die cast, and it was hoped that test data generated on these plates would be representative of the properties of those hemispheres.

Having fixed the test specimen geometry and recognizing the large number of test specimens required to explore the effects of the many process variables, the Research and Development Center was able to proceed with the design of a multiple-cavity, refractory metal die. The fact that the die was to be used on the Research Center's 200 ton Morton Vertacast Machine dictated that the die be designed around a central shot sleeve. The design that evolved incorporated three tensile test bars, six Charpy impact test bars, and two small plates, 1/8" thick, all in a radial array around the central shot sleeve. Thus, 11 die cast specimens could be produced simultaneously in a single shot, under one set of operating conditions.

The die cavities were to be sunk by a combination of conventional and electrical discharge machining into circular, single-piece cover and ejector-half impression blocks. As already noted, a molybdenum shot sleeve liner was to be copper-brazed to the cover-half impression block. Alignment across the parting plane of the die was to be provided by two hemispherical plugs attached to the cover die, which were to seat in corresponding depressions in the ejector die. The molybdenum impression blocks were to be held in their corresponding die housings by steel retainer rings. The retainer rings were designed to fasten to the die housing with screws accessible from the die face, permitting removal of the impression blocks without a complete disassembly of the die.

Figures 1 and 2, which may be found near the beginning of this report, illustrate very well many of the design features discussed above. Figure 63 discloses the design of the refractory metal components of the die in detail.

The die was designed to be heated by four cartridge heaters inserted in the steel heater plates behind each impression block.* Channels for these heaters were to be drilled completely through the die housing and then to be plugged at one end. A wheel-like insulation package, illustrated by Figure 64, was designed to fit behind each heater plate and provide rigid spacing while minimizing thermal conduction by reducing the metal-to-metal contact area. AISI 4340 was specified for the skeleton of the

*Watlow Firerods, manufactured by Watlow-Electric Manufacturing Company, St. Louis, Missouri, were specified.

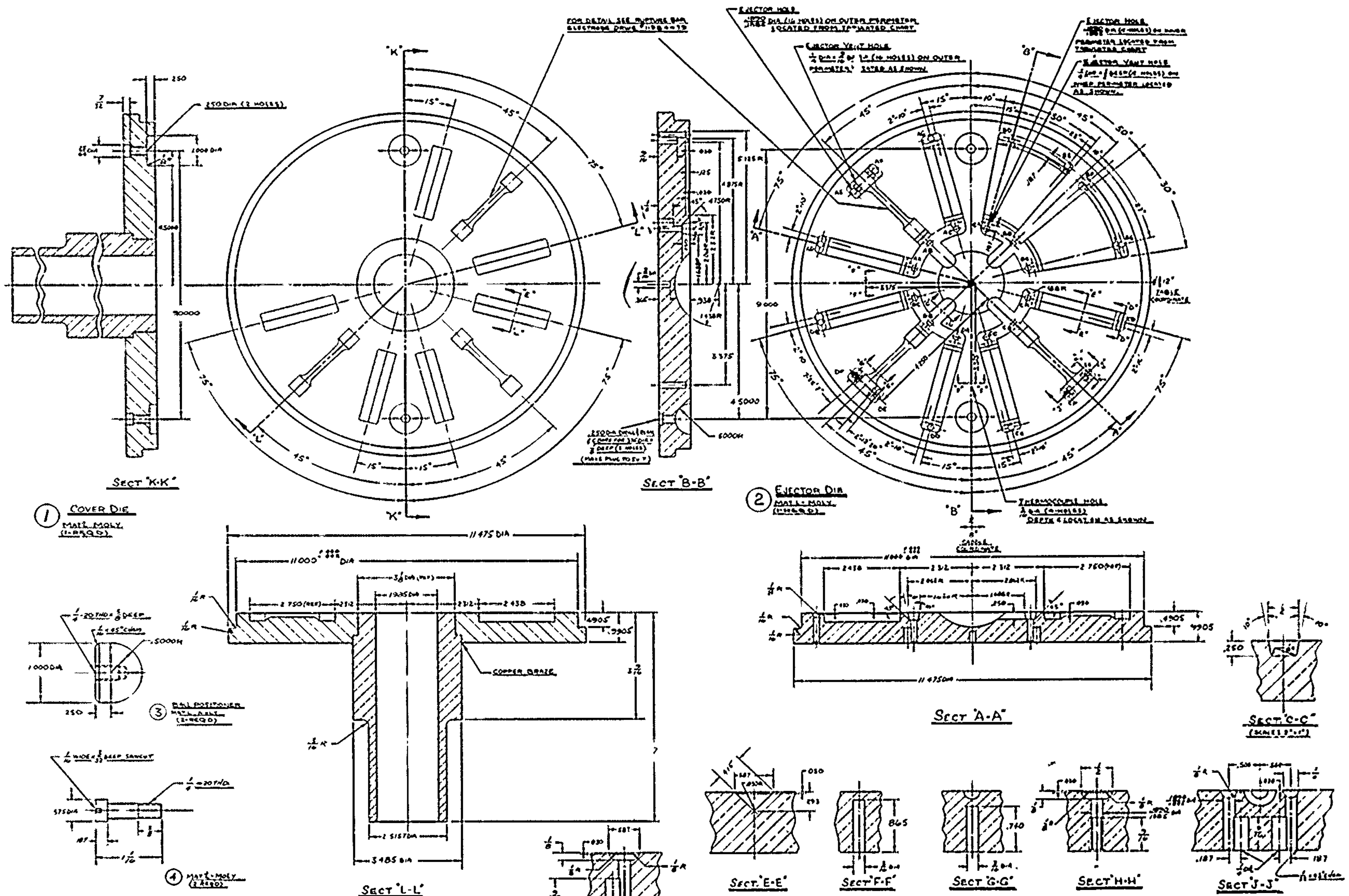
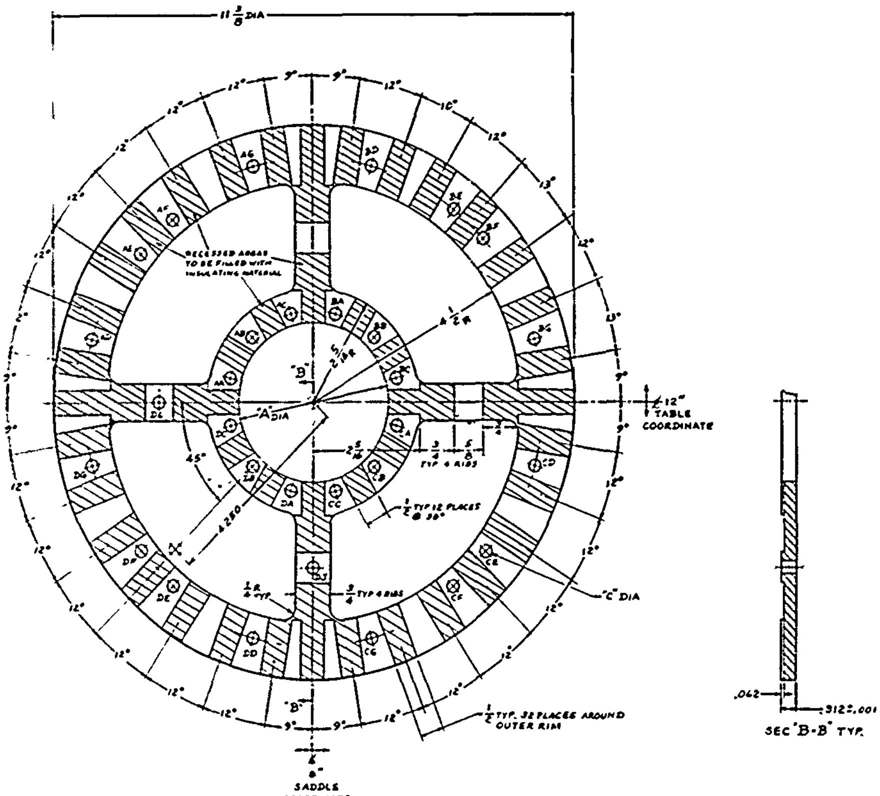


Figure 63 - High-density, pressed and sintered molybdenum components designed for the Research and Development Center test-bar die.



MAT'L 4340 STEEL
HEAT TREAT PER INSTRUCTIONS
ANGULAR TOLERANCES ARE NOT ACCUMULATIVE

DESCRIPTION	PT NO	"A" DIA	"C" DIA 38 HOLES	NO.
INSULATION (EJECTOR)	1	$3\frac{1}{4}$	[illegible]	1 REQ.
INSULATION (COVER)	2	[illegible]	OMIT	1 REQ

Figure 64 - Wheel-like, steel insulation package designed for the Research Center test bar die.

insulation package. The recesses were to be filled with rigid insulating board. Additional insulation, in the form of 1/2" thick rigid insulating board, was also provided between the "top" clamping plate and the stationary platen, and between the support plate and the ejector die housing.

Earlier work at the Research Center had revealed a distortion of the ejector pin holes at the surface of the die. This distortion was inevitably found on the biscuit side of the holes, suggesting that the bosses caused by short ejector pins tended to be drawn toward the center of shrinkage, thus deforming the relatively soft molybdenum prior to and during ejection. Steel ejector pins which extended into the cavity, on the other hand, were expected to be prone to soldering or to deteriorate rapidly, due to exposure to the molten metal. Thus, it was deemed important to have ejector pins precisely flush with the parting surface.

Because steel pins have a different coefficient of thermal expansion than molybdenum dies, and because both operate in a temperature gradient, it was considered impractical, if not impossible, to calculate the pin lengths required with sufficient precision to attain the desired result. It was obvious, therefore, that provision had to be made for a final adjustment of the length.

Withdrawal of the ejector pins is determined by the movement of the ejector plate. When the dies are closed, the withdrawal (upward movement) of the ejector plate and the ejector pins is limited by four stop pins on the top of the ejector plate which strike the ejector housing. To control the extension of the ejector pins, the Research Center's design made provision for variable stops (in the form of interchangeable stop-pads) to be mounted on the ejector housing directly above the stop pins. (This problem and the design solution were recognized by the Research and Development Center in prior work unrelated to this contract.)

The die design discussed above served as the basis for the construction of a test bar die by the Research and Development Center. A photograph of that die is presented as Figure 65.

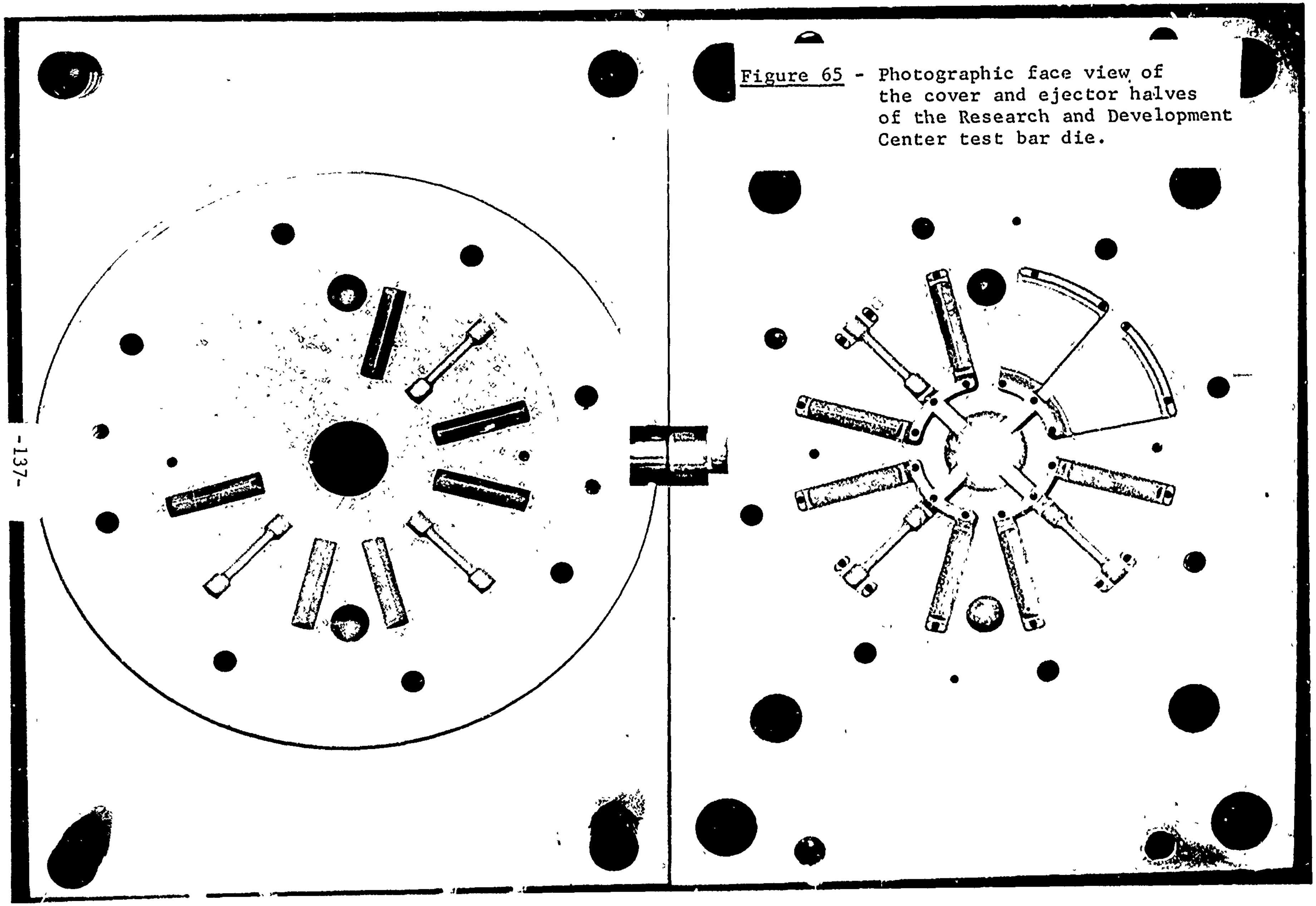


Figure 65 - Photographic face view of the cover and ejector halves of the Research and Development Center test bar die.

SECTION V

OPERATIONAL EXPERIENCE

As noted previously, each of the four organizations that were major contributors to this project set up and operated independent ferrous die casting facilities. The Dort Metallurgical Company was assigned responsibility for exploring metal injection and transfer systems and generating economic data based on a pilot production run; the Lamp Metals and Components Department assumed responsibility for the evaluation of die materials. For those organizations, the background art that was built up during the performance of their respective tasks was only incidental to their primary objectives and will not be included here, but will be reported incidentally in the sections devoted to those facets of the project which were the direct objective of the work being reported. Among their other responsibilities, however, the Doehler Jarvis Division of the National Lead Company was to determine the effect of die design on die performance and casting quality and to determine the effect of casting parameter variations on the quality of the castings. Overlapping responsibility for the determination of the effect of casting parameters on casting quality was also assigned to the General Electric Company Research and Development Center. It is those facets of the work performed by Doehler Jarvis and the Research Center which are reported here as operational experience.

1. INTERIM RESEARCH CENTER PROGRAM CONDUCTED IN COOPERATION WITH DORT

As noted in Section IV, the Research and Development Center pursued a joint effort with the Dort Metallurgical Company to expedite the Research Center's performance of their assigned task prior to the completion of their prime test bar die. In the course of that interim effort, a total of nine casting runs were made, producing over 600 specimens from a total of 188 shots. Castings were made from AISI 304 and 403 stainless steels, AISI 4340 steel, and cast iron compositions corresponding to 2.5%, 2.8%, 3.1%, and 4.4% carbon equivalent. Table XV summarizes the casting performed in the cooperative program.

The castings were produced on a new 400 ton Lester horizontal die casting machine. The design of the die was discussed in Section IV and was illustrated by Figure 46 which is, in effect, rotated 90°. The horizontal shot entered the bottom of the die and communicated with a vertical central runner which channeled the metal into the cavities above. Figure 66 is a photograph of a 2.5% CE malleable iron casting made in the interim test bar die. Two features of that photograph are most interesting. The first is the heavy flash, running the length of the runner, perpendicular to the plane of the photograph. The second feature of note is that the left-hand side of the casting appears to be cracked.

Table XV--Summary of the Castings Made in the Cooperative Program Between Dort and the Research Center

Date 1968	Alloy	No. Shots	No. Samples	Comments
6 March	304 stainless	13	32	Did not fill
7 March	Cast iron 2.5% CE	24	108	CE 2.63% (start), 2.30% (end)
8 March	403 stainless	25	126	
16 April	Cast iron 3.1% CE	30	90	
17 April	Cast iron 4.4% CE	26	78	Many samples broke on ejection
17 April	Cast iron 2.8% CE	24	72	CE 2.86% (start), 2.67% (end) .005% aluminum
18 April	4340 steel	25	75	Silicon and aluminum killed
18 April	403 stainless	15	45	Aluminum killed; used all available melt stock
19 April	Cast iron 3.7% CE	9	27	Many samples broke; biscuit frequently molten
	TOTAL	188	653	

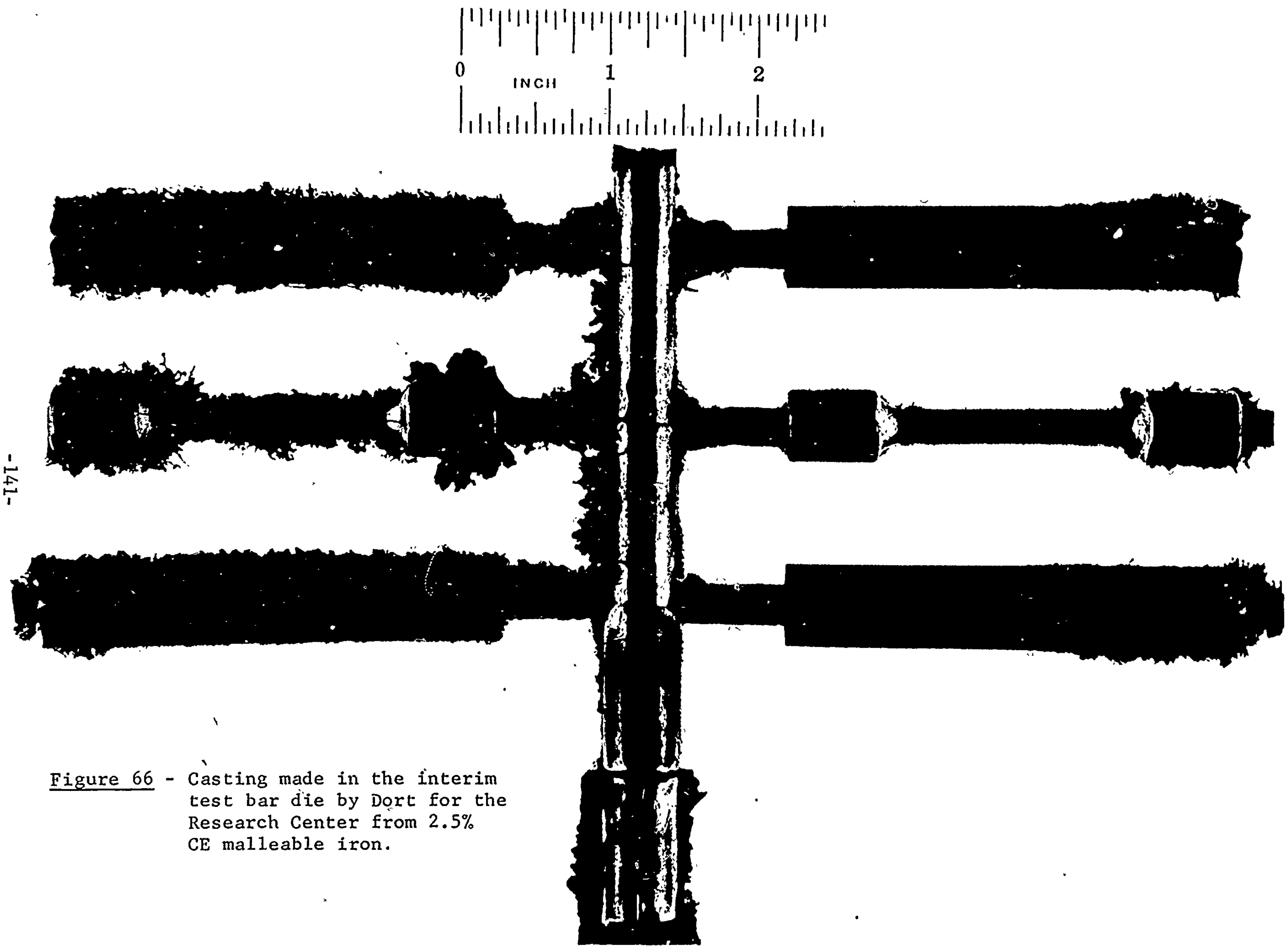


Figure 66 - Casting made in the interim test bar die by Dort for the Research Center from 2.5% CE malleable iron.

The recurrent formation of heavy flash along the runner on either side of the casting resulted from an opening up of the joint between the adjacent impression blocks. The effect of the flash was to make ejection of the part most difficult.

In search of an explanation for the problem, a straight edge was laid across the two impression blocks. The center of the die was observed to be higher than either edge, giving the impression that the blocks were not co-planar. That suggested that the impression blocks had expanded more than the surrounding A and B plates and had been forced to buckle, to accommodate that expansion difference. The fact that the die was heated by inserting a 20 KW electrical resistance heater between the cover and ejector halves gave credibility to the hypothesis, because it suggested that the refractory metal impression blocks may initially have been at a considerably higher temperature than the steel components of the mold base. Further, no supplemental heating at all was applied during the casting operation, reliance being placed on the sensible heat and the heat of fusion of the castings to maintain the die temperature. That mode of operation was assumed to result in the impression blocks reaching a higher temperature than the A and B plates. If the assumption were true, it, too, would seem to corroborate the hypothesis.

However, because the coefficient of thermal expansion of the refractory metal impression blocks was less than half that of the steel A and B plates, and because the impression plates were relatively thick and had square edges, the buckling hypothesis was viewed with skepticism. One test of the hypothesis was to calculate the critical strain required for buckling. The critical strain can be calculated from a modification of Euler's equation for a rectangular column with one end fixed and one end free:

$$\text{Critical strain} = \frac{\pi^2}{24(L/d)^2} = 0.0257,$$

(where L/d, the ratio of the width of the blocks to their thickness, is approximately four). If the dimensions of the cavity remained fixed, the actual strain in the impression blocks would be the product of the coefficient of thermal expansion times the temperature rise from room temperature to the operating temperature. The coefficient of thermal expansion of molybdenum is approximately $4.9 \times 10^{-6}\,°C^{-1}$. Therefore, a temperature rise of approximately 5250°C would be required to buckle the impression block. This is an obvious absurdity and effectively discredits the hypothesis that the impression blocks were buckling.

It had been observed previously that whenever interfaces between high-density, pressed and sintered molybdenum die components were exposed to high temperature molten metal (e.g., brass or ferrous alloys), they suffered

a localized deformation in the form of an ever-widening gap between the components; and the same mechanism was, undoubtedly, also active in the formation of the gap between the paired impression blocks of the Research Center's interim test bar die. That mechanism does not, however, account for the nonplanar relationship observed between the impression blocks forming the face of the die. The explanation for that observation remains a mystery. Whatever the mechanism, it can be stated categorically that, for high temperature alloy die casting, good die design minimizes the length of interface between die components exposed to molten metal.

The second feature of note in Figure 66 is that the left-hand side of the casting appears to be cracked. In reality, the apparent cracks are a manifestation of cracks in the left-hand impression block, not cracks in the casting. The cracked impression block was constructed from an 80% molybdenum, 20% tungsten binary alloy in the wrought condition. It was the anisotropy of the mechanical properties created by the process of plastic deformation that accounts for the abvious fact that the cracks which developed exhibited a preferred orientation. The cracking itself may be attributed to the effects of thermal fatigue on a material having insufficient ductility. The uncracked right-hand impression block was constructed from high-density, pressed and sintered molybdenum. Its superiority over the 80% molybdenum-20% tungsten alloy is obvious.

When the die was disassembled after the 8 March 1968 run, the cracked 80% molybdenum-20% tungsten alloy impression block literally fell apart. It was not replaced. That impression was blocked off. To avoid a repetition of the difficulties associated with gap formation in the runner, a high-density, pressed and sintered molybdenum runner block was inserted. The interface between the impression block and the runner block was exposed to molten metal only at the three points where it was intersected at a 90° angle by the three in-gates. This design modification proved to be much more satisfactory than the original design. Figure 67 is a photograph of a casting made subsequent to the modification.

The surface quality of all of the castings was quite good. The variations noted were attributed to slight variations in the application of the die release agent.

All the castings produced at Dort were radiographed. They were all revealed to be riddled with porosity and completely unacceptable for mechanical testing. No correlation could be found between porosity and alloy composition, pouring temperature, die temperature, plunger velocity (in the range of 20" to 40" per second) or gating variations.

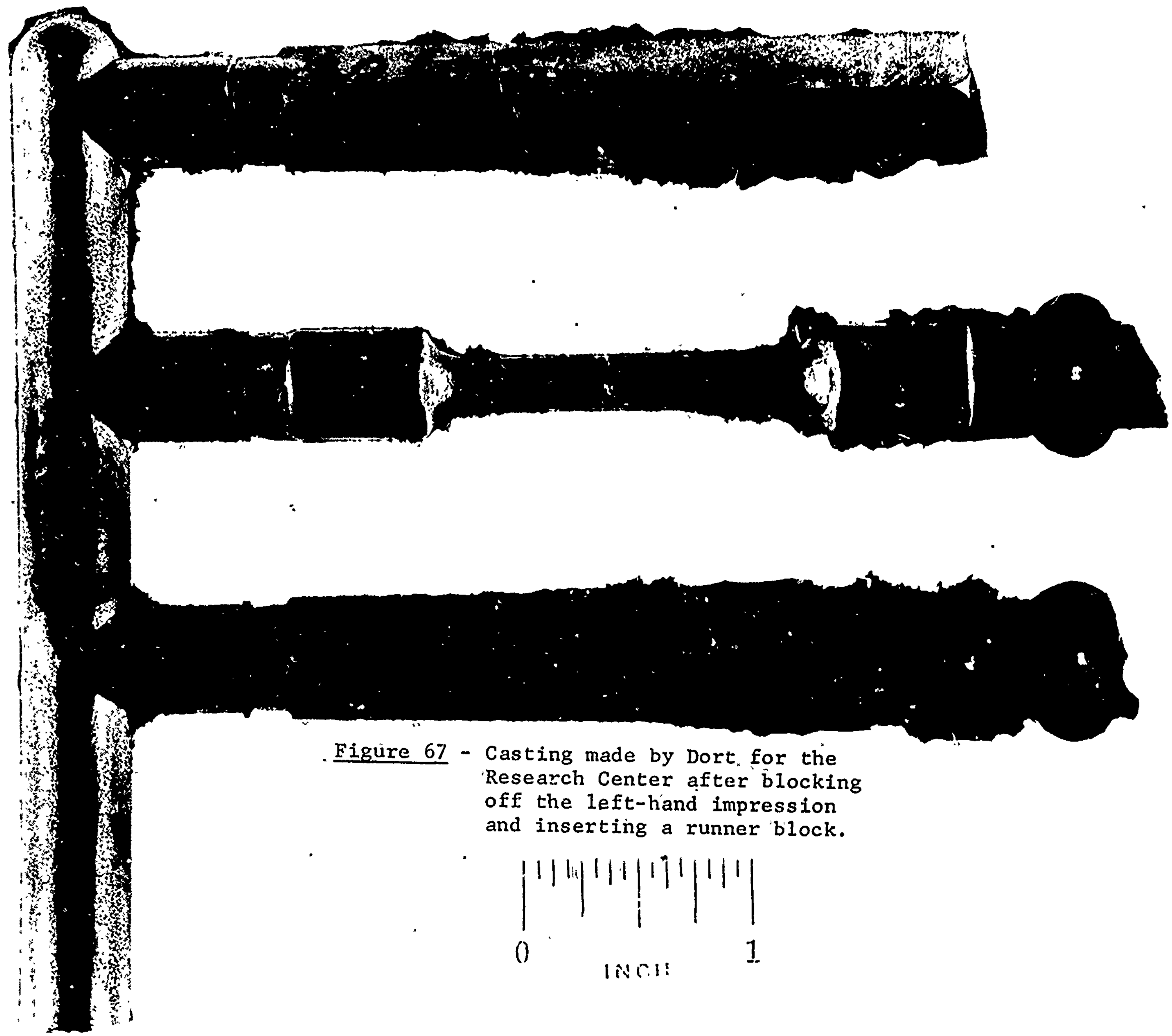


Figure 67 - Casting made by Dort for the Research Center after blocking off the left-hand impression and inserting a runner block.

(The in-gates were progressively enlarged, from 0.030" thick by 0.250" wide on the sixth of March, to 0.250" diameter on the seventh and eighth of March, to 0.500" diameter for the runs made on the sixteenth through the nineteenth of April. The overflow gates, which were initially 0.030" thick by 0.250" wide, were enlarged before the April runs, to 0.060" thick by 0.500" wide on one impact bar, and 0.120" thick by 0.500" wide on the other impact bar and the tensile bar.)

The Research Center concluded that the excessive porosity was largely due to entrapped air. In retrospect, it was suggested that the die designed for Dort's conventional horizontal die casting machine was particularly prone to air entrapment and that more favorable results might have been obtained with an improved die design.

Many of the features of the casting operation performed by Dort for the Research Center are also of interest. As previously reported, the die halves were preheated to 700°F to 900°F from their front faces. To expedite heat-up, and also to retain the sensible heat of the die during operation when no supplemental heating was available, Dort had insulated the external surfaces of the die assembly with Marinite board. In addition, the die was wrapped in an insulating blanket during heat-up. Typically, it required only three hours to bring the die up to the 900°F operating temperature. The shot sleeve was preheated with a gas torch for approximately an hour immediately before casting.

It was convincingly demonstrated that dependence could be placed on the heat surrendered by successive shots to maintain the temperature of the dies, if reasonable production rates were maintained. Die temperature variations were not excessive at average shot frequencies of 30 shots per hour. This is demonstrated by the charts reproduced in Figures 68 and 69, which record the die temperatures, measured at several different points, as a function of time. These charts also provide a good indication of the production rates attained. The practice of preheating the dies from the die face proved to be successful.

Melting was performed without a protective atmosphere in a 200 pound induction-heated furnace. Metal transfer was accomplished by pouring metal from the crucible into a red-hot plumbago hand ladle (preheated by gas flame) and transferring it manually to the shot sleeve pour hole. After pouring, the operator pressed a button to initiate metal injection. Transfer of metal by this method was a two-man operation.

Between each shot, the die faces were coated with acetylene black; the plunger was lubricated with Franlube KR-3 plunger lubricant;* and the shot sleeve was coated successively with Dycote 40 and EP-3463.**

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*Franlube KR-3 is a high-viscosity, petroleum-base product, pigmented with graphite and containing other extreme pressure agents--produced by LaFrance Manufacturing Company, St. Louis, Missouri.

**Dycote 40 (colloidal graphite in mineral oil) and EP-3463 (a mixture of graphite and magnesium silicate, formerly known as XS-1040B) are both products of Foseco, Incorporated, Cleveland, Ohio.

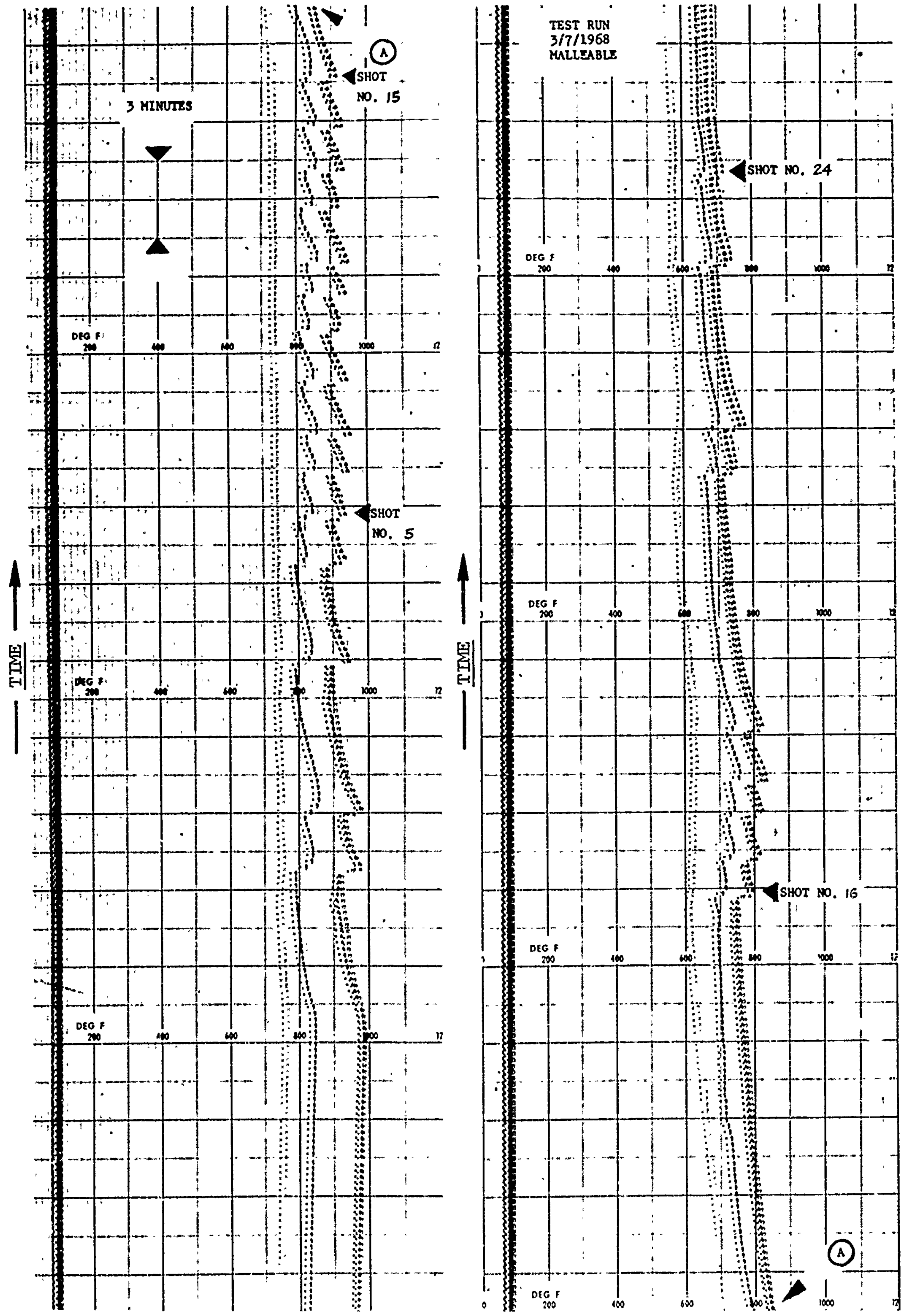


Figure 68 - Chart indicating the maintenance of die temperatures without supplemental heating at Dort.

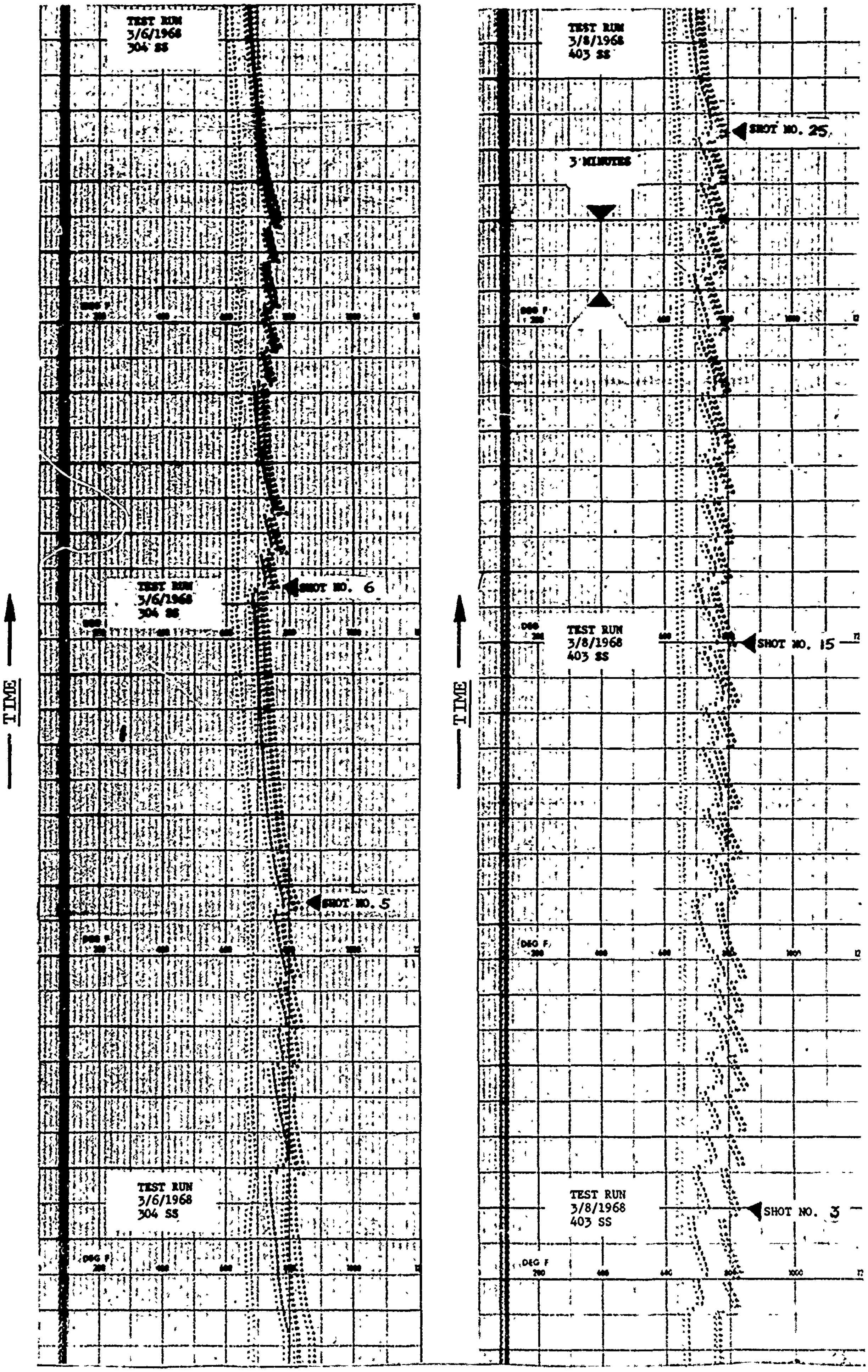


Figure 69 - Chart indicating the maintenance of die temperatures without supplemental heating at Dort.

The shot sleeve was fitted with a René 41 liner with a 1/4" thick wall. The water-cooled plunger was made from beryllium-copper. A 1/2", tight-fitting graphite wafer was inserted ahead of the plunger for each shot. The bore of the shot sleeve liner was nominally 2". A room temperature clearance of 0.010" on the diameter was employed between the plunger and the shot sleeve liner. (A 0.030" clearance had been found excessive in earlier work. It permitted the plunger to seize in the sleeve when metal leaked past the graphite wafer.)

To investigate the effect of plunger velocity, that parameter was varied from an estimated value of 20" to 40" per second. The pressure on the metal was calculated from the hydraulic pressure of 1000 psi to be 2850 pounds per square inch.

Production rates of 30 to 60 shots per hour were attained by hand ladling.

DOEHLER JARVIS' PROGRAM

After gaining materials handling experience by casting eutectic iron in an obsolete steel die designed for aluminum die casting, Doehler proceeded to put their test-bar die into service. The metal transfer and injection systems have been described earlier in this report. Process parameters were recorded on a Visicorder, 12-channel, light-beam oscillograph, manufactured by Honeywell. The inputs included: shot cylinder pressure, die temperatures, plunger position and velocity, cooling-water flow rate, and inlet and outlet cooling-water temperatures.

Doehler's test bar die, as reported earlier, was a two-cavity, combination tensile and impact bar die. A single, high-density, pressed and sintered molybdenum impression block in each half of the die contained both cavities and the entire gate and runner system. Like the hemisphere die, it was fitted with a full set of wrought molybdenum ejector pins.

In preparation for initial operation, the die was heated to 500°F. The two molybdenum pins nearest the in-gate feeder seized during trial operation, before the first shot had been made, arresting the ejection mechanism.

The die was disassembled. Efforts to free the frozen pins being unsuccessful, they were removed by jig boring.

An analysis of the problem suggested two possible causes:

a. A misalignment between the ejector plate and the ejector half of the die

b. An incompatibility between molybdenum ejector pins and the molybdenum die.

Even though the design of the ejection mechanism incorporated floating, self-aligning ejector pins, the alignment was carefully checked. No misalignment was observed, so two new molybdenum ejector pins were introduced to replace those bored out. The die was again preheated to 500°F and put through several trial cycles without introducing molten metal. One ejector pin seized, and a second broke.

The die was again disassembled. The situation was discovered to be similar to that observed earlier.

To determine if the substitution of steel pins for the molybdenum ejector pins would affect an improvement, a heavy molybdenum plate was bored to accept pins of each material. The plate was preheated, and the pins slipped back and forth. Graphite and molybdenum disulfide were each evaluated as lubricants for both the molybdenum and the D-M-E steel pins.

The molybdenum pins were found to be more prone to seizure than the steel pins, regardless of the lubricant. Graphite was found to be superior to molybdenum disulfide as a lubricant in this evaluation.

On the basis of this evaluation, Doehler replaced all of the molybdenum pins in both dies with D-M-E steel pins.

The test bar die was again reassembled and heated to 500°F, with the intention of making several aluminum shots to demonstrate that everything was working properly. The first aluminum shots were "oil shots," purposely made with excess lubrication. No difficulties were experienced, so Doehler proceeded to cast eutectic iron in the test bar die. The die temperature was maintained at 500°F.

After only three shots, serious welding was encountered in the gate area and at several other points in the die cavity. As a result of this welding, a piece of molybdenum about 3/32" wide by 3/4" long broke away from the intersection of the parting plane and the tensile-bar cavity when the casting was ejected. Figure 70 is a photograph of the die and the casting with the extracted molybdenum still firmly welded to it.

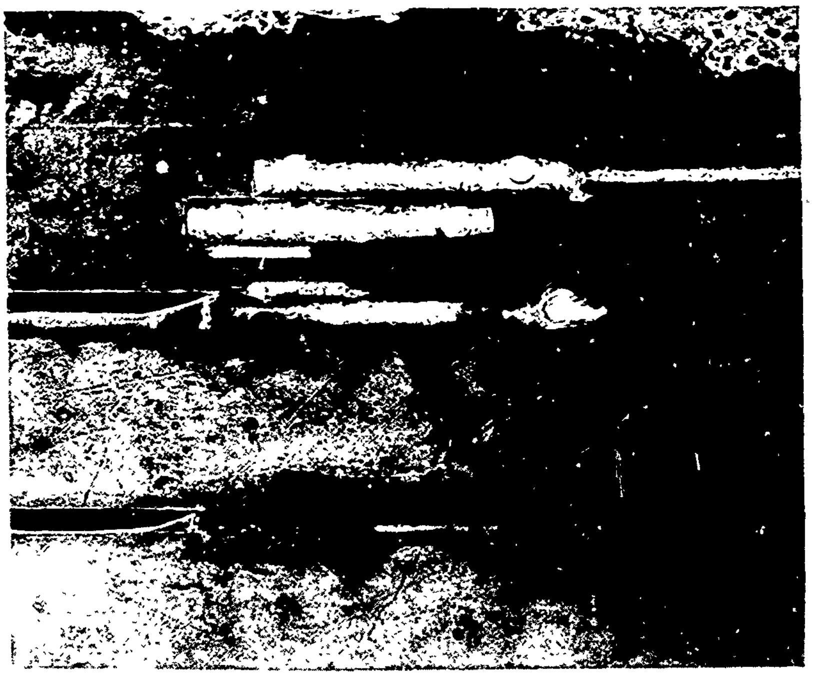


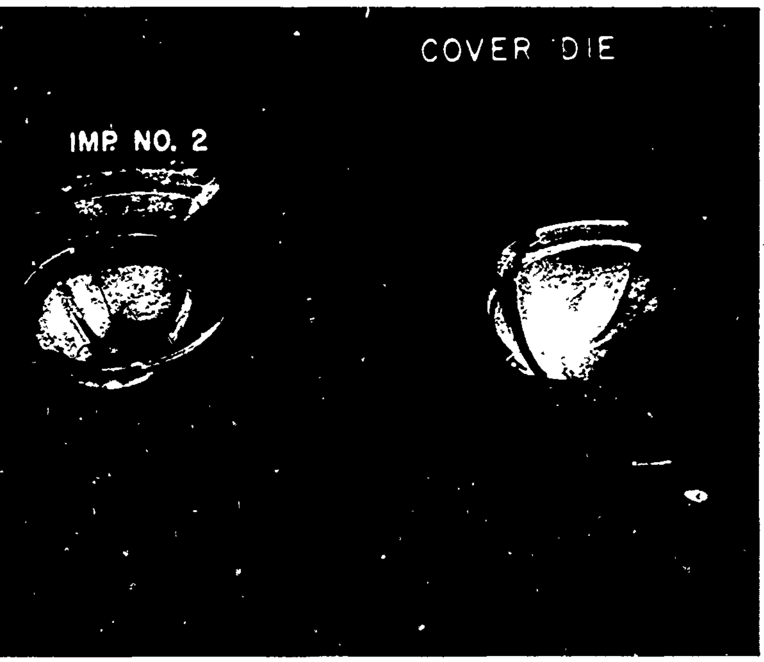
Figure 70 - Photograph of Doehler Jarvis' test bar die and two tensile bar castings with the molybdenum which broke out of the die still welded to them.

It was concluded that the welding was related to an in-gate velocity too high for the die-release agent being used. Accordingly, the cross-sectional area of the tensile bar in-gate was increased by increasing the thickness of the gate from 0.040" to 0.180", and it was resolved to explore lower plunger velocities. To repair the tensile bar cavity, it was also necessary to increase the diameter of the shoulder closer to the in-gate from 7/16" to 9/16".

While these modifications were being affected, the hemisphere die was put into operation. On the twenty-second, twenty-third, and twenty-fifth of April, respectively, Doehler made 15, 36, and 13 shots of eutectic iron in the three-cavity hemisphere die. Under varied operating conditions, it was observed that increasing the die temperature increased the tendency to weld. At longer dwell times, the eutectic iron castings were cracking in the die and falling apart during ejection. This created a problem, because some fragments of the castings were not being ejected but were being left in the die. This problem was particularly troublesome with the hemisphere die. Decreasing the dwell time decreased the incidence of breakage in the gates and decreased the tendency for the white iron castings to crack; but it also increased the danger that the biscuits, insulated by the asbestos cup, would not have sufficient time to solidify and might burst (explode) upon being ejected. (Biscuit explosion represents a safety hazard, but it also, less obviously, introduces the risk that the molten metal spraying from the biscuit at high velocity will weld to the face of the die.)

Based on their experience at Dort, representatives of the Research and Development Center were able to recommend replacing the eutectic iron with either a 3.1% CE malleable iron (2.5% carbon, 1.5% silicon, 0.5% manganese, balance iron) or stainless steel, neither of which would crack in the die or fall apart during ejection.

After 15 shots in the hemisphere die, a hairline crack was noted in cover impression Block 2. This block was constructed from pressed and sintered tungsten strengthened with a dispersion of two weight percent thoria. (This is abbreviated to W-2%ThO_2.) The crack grew progressively as additional shots were made. During the second day's run, a casting welded to the W-2%ThO_2 and stuck in the cavity. When it was removed, it broke a piece of the die out of the bottom of the cavity, rendering the W-2%ThO_2 cover impression block useless. Figure 71 is a photograph of the casting before it was removed. For the third day's run, that cavity was blocked off by inserting a casting in it.



<u>Figure 71</u> - Photograph of a cracked hemisphere casting, with overflow, stuck in a tungsten-2% ThO_2 cover die impression (on left).

On the thirtieth of April and the first of May, Doehler substituted 3.1% CE malleable iron for eutectic iron (as suggested by the Research and Development Center) and continued casting in the three-cavity hemisphere die with one impression blocked off, making 25 and 29 shots, respectively. The operating conditions were:

Metal Temperature	2600°F-2780°F
Die Temperature	600°F-645°F
Pressure on Metal	4000-4400 psi
Plunger Speed	7.5 ips
Dwell Time	3.1-4 seconds
Plunger	Water-Cooled

The campaign on the hemisphere die was discontinued, because the castings were hanging up in the gaps which were opening at every interface between an impression block and a runner block or insert which was exposed to high pressure molten metal. These gaps can be seen in both the cover and the ejector dies, as illustrated in Figures 72, 73, 74, and 75. (Figures 73 and 74 also show more clearly than Figure 71 the cracks in the W-2%ThO_2, cover impression Block 2.)

The following materials were incorporated in the hemisphere die, as illustrated by Figures 71 through 75:

<u>Cover Half</u>

Impression Block 1	High-density, pressed and sintered molybdenum
Impression Block 2	Pressed and sintered tungsten plus 2 weight percent ThO_2
Impression Block 3	Pressed, sintered, and extruded molybdenum
Gate Block	High-density, pressed and sintered molybdenum

<u>Ejector Half</u>

Impression Block 1	Entire noninserted block made from a single piece of high-density, pressed and sintered molybdenum

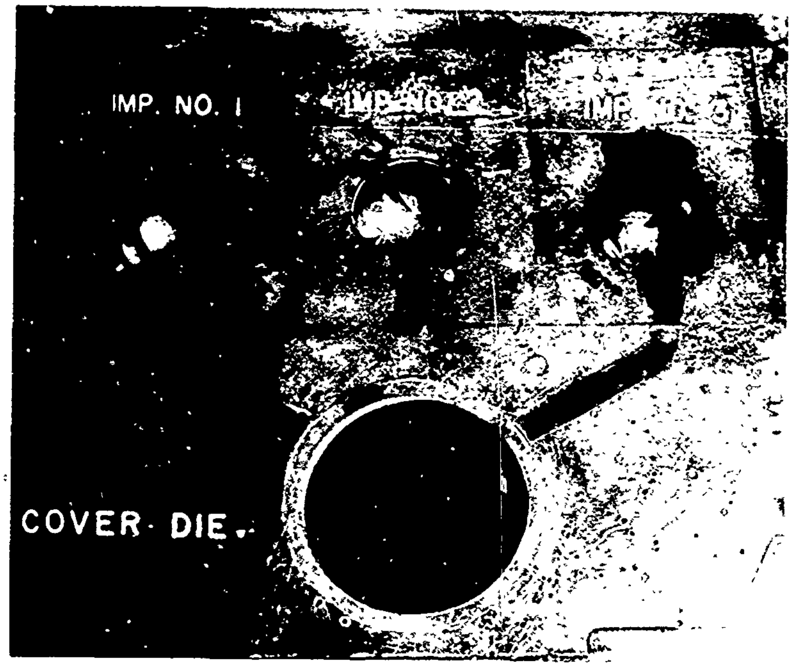


Figure 72 - Cracks and gaps at interfaces in the hemisphere cover die.

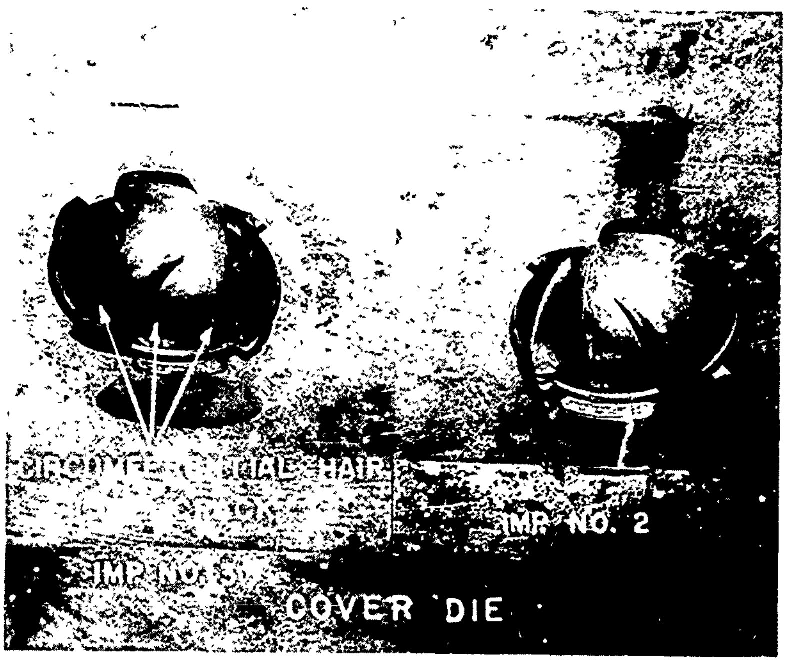


Figure 73 - Cracks and gaps at interfaces in the hemisphere cover die.

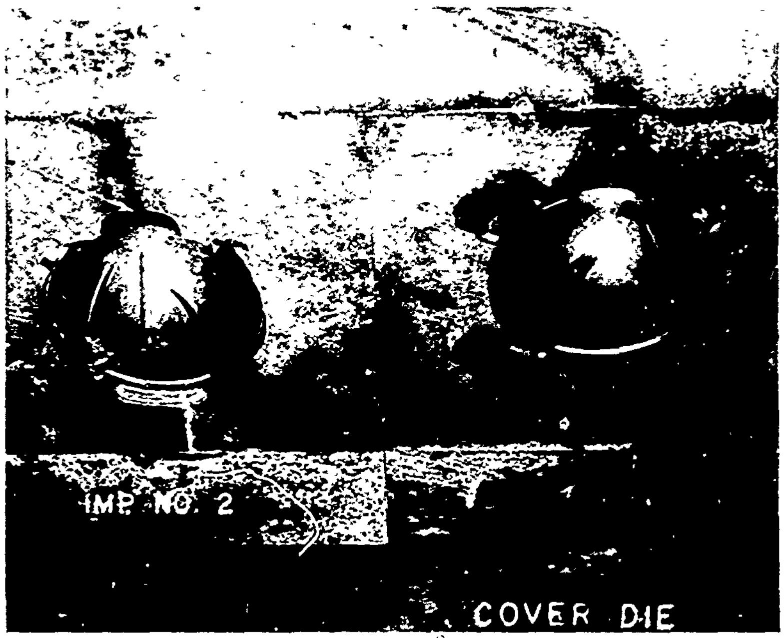


Figure 74 - Cracks and gaps at interfaces in the hemisphere cover die.

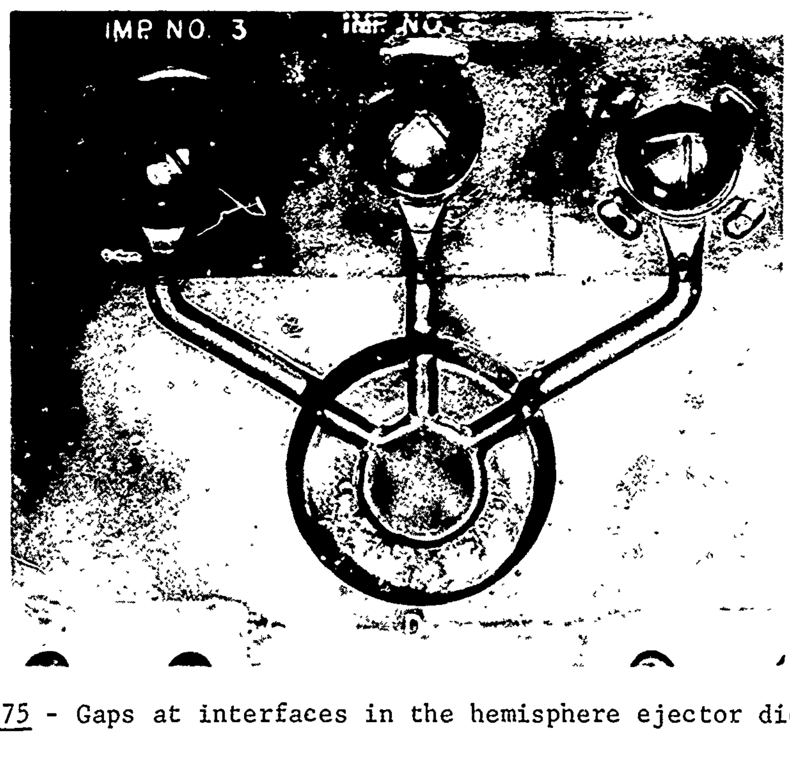


Figure 75 - Gaps at interfaces in the hemisphere ejector die.

Impression Insert Retainer 2	High-density, pressed and sintered molybdenum
Impression Insert 2	High-density, pressed and sintered molybdenum
Impression Insert Retainer 3	High-density, pressed and sintered molybdenum
Impression Insert 3	Rolled, powder metallurgy molybdenum bar
Gate Block	High-density, pressed and sintered molybdenum.

Doehler's hemisphere die featured oblique screws, designed to compensate for the substantially different coefficients of thermal expansion of the refractory metal and steel die components. The design was a success. The die could be heated without forming separations at the interfaces. However, as mentioned above, gaps formed locally, wherever an interface was exposed to molten high-pressure metal. Flash formed in these gaps, interfering with ejection and preventing sustained production.

At this point, it is well to review the mechanism that is believed to account for the formation of gaps at interfaces wetted by the high pressure molten metal. Although thermal ratcheting mechanisms have been described which might explain this phenomenon, a far less subtle mechanism is suggested. The thermal pulse induced in the surface of the die by the inrushing metal would be accompanied by an expansion of the surface of the die, were it not constrained by the cooler underlying material. At a joint, however, that constraint is not symmetrical, and during the first shot (or cycle), the edges of the two adjacent die components become plastically deformed (or upset). This is possible in the areas exposed to the molten metal, because the thickness increase concomitant to the foreshortening of the heated surface of the die can be accommodated as an expansion into the cavity or runner.

Were this a strictly thermal phenomenon, the mechanism described above would be active only during the first cycle. The introduction of molten metal as the source of heat, however, changes the complexion of the situation. It can easily be hypothesized that, on the second shot, molten metal fills the imperceptibly small gap formed during the first shot and solidifies as flash, while the surface temperature of the die is still increasing. This flash acts as a wedge which is driven deeper into the expanding gap with each successive shot, permitting the suggested mechanism to act on every shot.

Several possibilities for the elimination or circumvention of this problem were recognized. Perhaps the most obvious was to eliminate interfaces by constructing each half of the die from a single piece of material, as in the Doehler Jarvis test bar die. Another potential solution was to eliminate interfaces by brazing, as was done in the Research and Development Center test bar die. Doehler Jarvis designed a self-cleaning joint to combat the problem. (That approach was illustrated by Figure 44.) Finally, a die material with a higher yield strength than high-density, pressed and sintered molybdenum might deform only elastically, and not plastically, at interfaces and, therefore, not be subject to this problem.

Doehler put the test bar die back into service on the second of May. On the second, third, and sixth of May, respectively, 11, 52, and 6 castings were made from 3.1% CE malleable iron. Unlike the earlier experience with eutectic iron, it was found possible to eject the complete casting, including the tensile bar, the impact bar, gates, runners, and the biscuit. Surface cracks were reduced but still present. Radiography, however, indicated severe porosity on all tensile and impact bars.

This series of runs was terminated when the beryllium-copper plunger seized in the molybdenum shot sleeve. (See Figures 42 and 43.)

On the sixteenth of May, three shots were made with the self-cleaning steel-shot sleeve. The third shot struck in the ejector half of the die, and a small piece of molybdenum broke out of the runner entrance to the ejector die when the casting was removed. The die had to be repaired again.

Until that time, the operation of the test bar die had been restricted to the following range of variables:

Metal Temperature	2600°F-2650°F
Plunger Speed	5-7.5 ips
Pressure on Metal	4400 psi
Die Temperature	500°F-600°F
Dwell Time	2.7-4 seconds

Water-cooled beryllium-copper plungers were invariably employed. If the dwell time were limited to less than three seconds, crack-free castings were produced. Longer dwell times resulted in cracked castings. All of the castings contained excessive porosity.

In an attempt to minimize porosity, the runs made on the twentieth and twenty-first of May explored higher injection pressures. The pressures employed ranged from 4800 psi to as high as 24,000 psi. For pressures above 10,000 psi, pressure intensification was employed.

Radiography showed a definite reduction in the test bar porosity. According to the Visicorder recordings, however, the full intensifier pressure was built up 0.4 seconds after completion of the shot--possibly too late to exert its full beneficial effect on the solidifying casting. Therefore, it was decided to adjust the shot control, in order to accelerate the intensifier pressure build-up.

At Shot 28, on 21 May, with the injection pressure still at only 4800 psi, more small die breakouts occurred in the runners in the ejector die, as illustrated photographically by Figure 76.

On the twenty-fourth, twenty-seventh, and twenty-eighth of May, respectively, 32, 31, and 29 shots were produced. With the pressure intensification system adjusted to minimize the response time, extremely high injection pressures were evaluated in combination with higher injection velocities, higher die temperatures, and higher melt temperatures. The injection pressures investigated ranged from 17,600 psi to 32,000 psi; the plunger speed ranged from 2 ips to 20 ips; the die temperature ranged from 550°F to 820°F; and the melt temperature ranged from 2600°F to 2700°F. Several shots were also made from aluminum on the twenty-fourth of May as standards of comparison.

Doehler reached several general conclusions, based on their results to that point. Increasing the metal pressure and the injection velocity improved the surface appearance of the castings and reduced the amount of internal porosity that could be observed in the castings by X-ray inspection. Higher pressures and velocities also increased the propensity for soldering. Whereas increased die temperatures and metal temperatures did not noticeably improve the quality of the castings, it was clear that increasing those parameters, too, did stimulate soldering.

During the course of the three days' runs just described, the tendency to solder (weld) increased noticeably. Small particles of molybdenum continued to be pulled out of the die, making ejection and casting removal most difficult.

The test bar die was thereupon returned to the die shop to be repolished. Under the assumption that high gate velocities were responsible for the welding, the gates of the test bar die were further enlarged to a thickness of 0.300" from 0.180" (originally they had been 0.040").

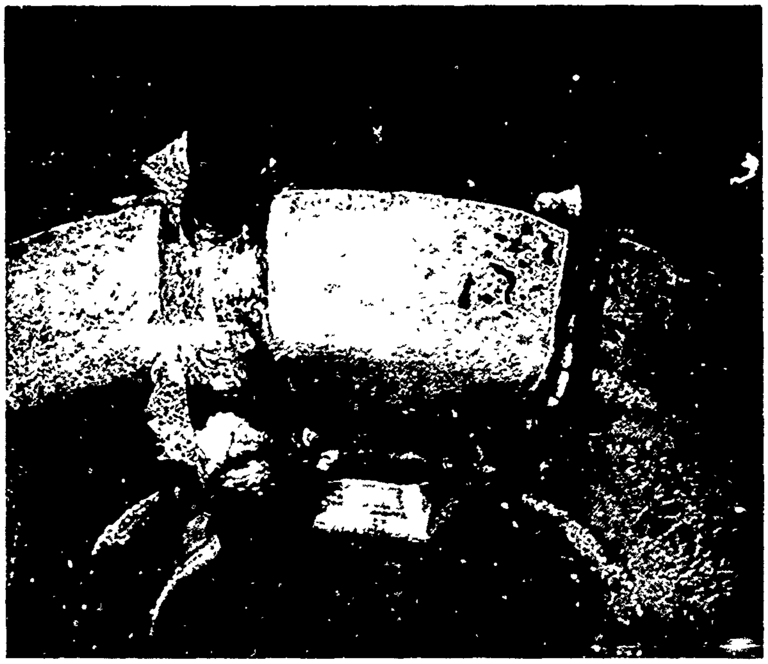


Figure 76 - Break-outs in the runners in the ejector half of Doehler's high-density, pressed and sintered molybdenum test bar die. (This area is opposite the self-cleaning shot sleeve.)

One of the castings, which had welded to the die and broken out a piece of molybdenum, was sectioned through the molybdenum fragment. Figure 77 is a photomicrograph of that section. Alloying has obviously occurred between the molybdenum (white area) and the cast iron. Examination at 1000X confirmed that conclusion. It is not surprising, however, in view of the fact that the minimum melting point in the iron-molybdenum binary system is 2624°F,[14] well within the range of pouring temperatures investigated by Doehler. (If they effect the solidus at all, the carbon, silicon, and manganese additions to the malleable iron would be expected to reduce the solidus of the five-component molybdenum-malleable iron system even more.)

For any given die casting alloy combination, the tendency to solder or weld is related not only to such process parameters as injection pressure, injection velocity, die temperature, and pouring temperature, but also to the die release agent selected. Throughout their program, Doehler depended heavily upon Glas-Mol 7,* alone or in combination with other release agents. When properly applied to a die surface at 600°F, Glas-Mol forms a thin tenaciously adherent film. (At the end of May 1968, Doehler was periodically applying Glas-Mol to their die and then superimposing a coating of acetylene black prior to each shot.)

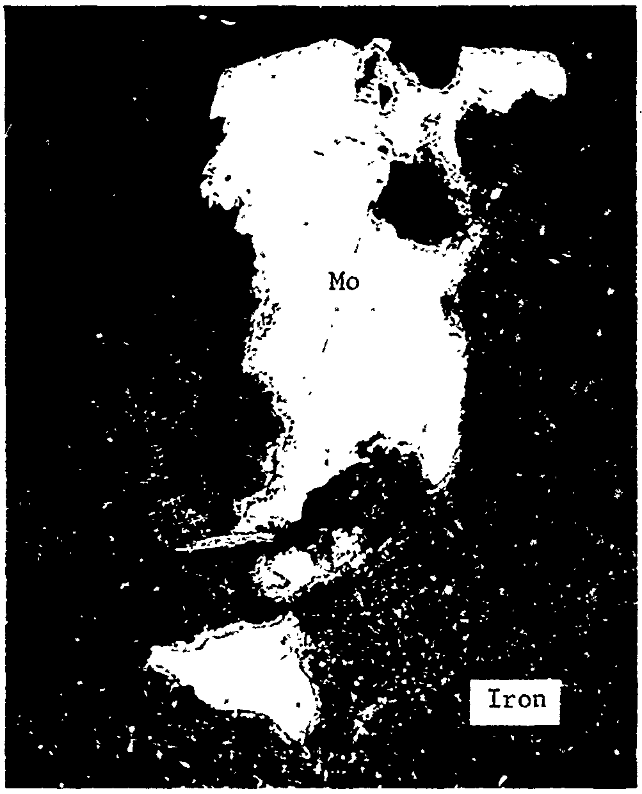
While running malleable iron in the hemisphere die at Doehler, the melt was observed, with the passage of time, to become sluggish, as though it were about to solidify. This has since been attributed to a loss of carbon, which increases the liquidus of the iron. Doehler eliminated this difficulty by periodically adding carbon in the form of graphite granules to the melt, by installing a cover over the furnace, and by floating pieces of graphite electrode on the surface of the bath, thus creating a neutral or reducing atmosphere above the bath.

After repairing the test bar die, Doehler made 17 shots of 3.1% CE malleable iron on the fourth of June. The following conditions were employed:

Metal Temperature	2610°F-2780°F
Plunger Speed	4-5 ips
Pressure on Metal	17,600 psi
Die Temperature	600°F-680°F
Dwell Time	4 seconds

The castings tended to hang on the ejector pins but could be removed with ease. At Shot 14, it was noticed that particles of die material from the ejector half were removed when ejecting the casting. The die had to be repolished and the gate feeders ground larger to remove pitting marks and cracks in the ejector die half.

*Glas-Mol is basically a water suspension of molybdenum disulfide produced by the Dynamics Research and Development Corporation, Toledo, Ohio.



Mag. = 60X

Figure 77 - Photomicrograph of a fragment of molybdenum which has welded to a 3.1% CE malleable iron casting and broken out of the die.

On the sixth of June, 67 shots were made with malleable iron under the following conditions:

Metal Temperature	2610°F-2680°F
Plunger Speed	4 ips
Pressure on Metal	2800-6000 psi
Die Temperature	600°F-680°F
Dwell Time	4 seconds

On this run, the runner area in the ejector die showed progressively serious deterioration, as illustrated by Figures 78 and 79. (The castings were numbered consecutively. Numbers 1, 10, 20, 30, 38, 51, and 67 are illustrated.) Again, small particles of the die material were torn out.

On the seventh of June, Doehler Jarvis made 21 shots of 304 stainless steel in the test bar die under the following conditions:

Metal Temperature	2900°F-3070°F
Plunger Speed	3-12 ips
Pressure on Metal	4400-17,600 psi
Die Temperature	635°F-700°F
Dwell Time	4 seconds

No deterioration of the die materials was noticeable.

On the tenth of June, 44 shots were made with 403 stainless steel under the following conditions:

Metal Temperature	2900°F-3050°F
Plunger Speed	4-17.5 ips
Pressure on Metal	4400-17,600 psi
Die Temperature	600°F-700°F
Dwell Time	3.9 seconds

Ejection was easy, and again, no break-outs were noted in the dies. During the course of this run, a heavy slag was observed to build up, and heavy skulls were left in the ladles.

The persistent welding problems experienced with eutectic and malleable irons by Doehler Jarvis were markedly relieved by substituting stainless steel for cast iron. Based on that observation, Doehler was authorized by the Lamp Metals and Components Department, on the fourteenth of June, to discontinue casting malleable iron and to substitute stainless steel for it.

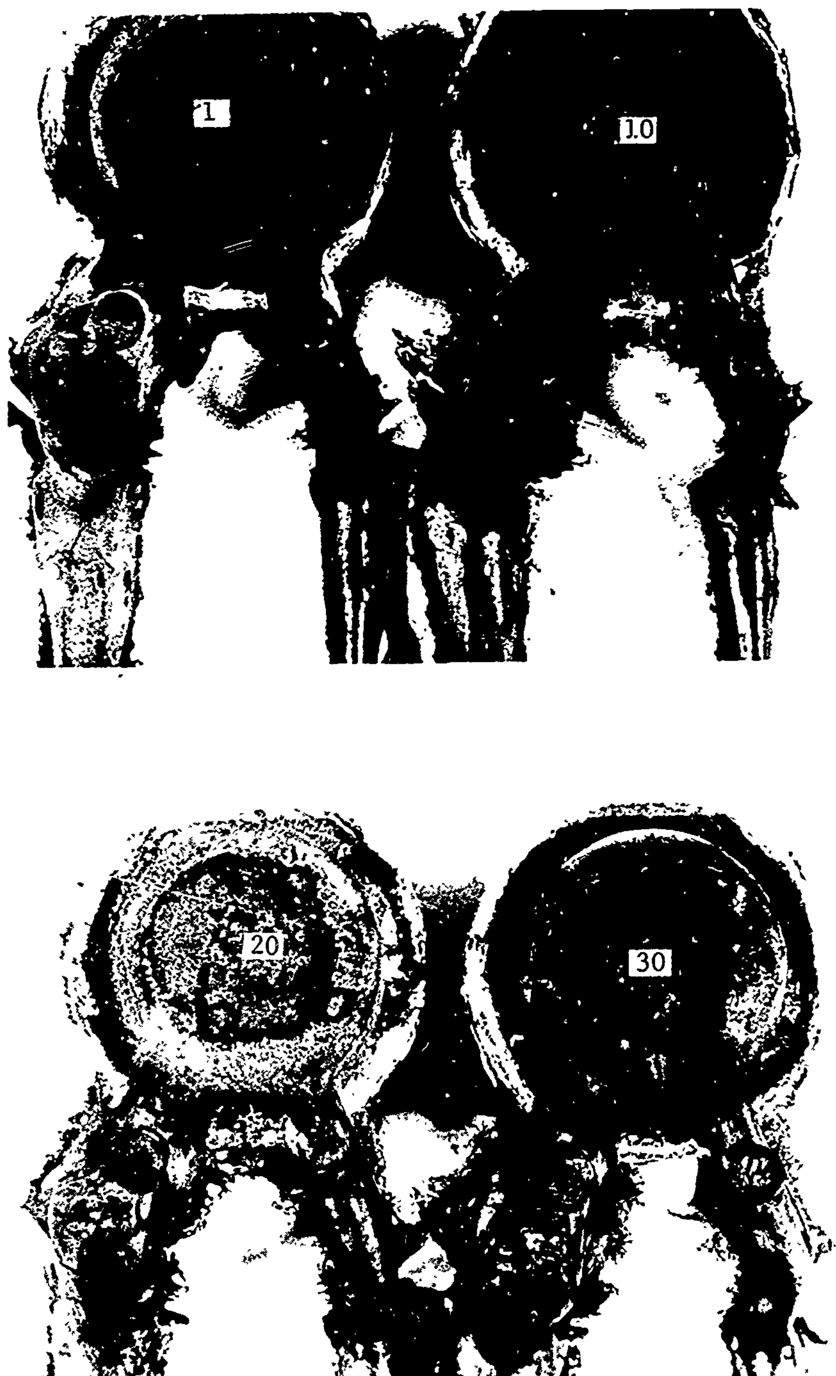


Figure 78 - Castings made in the Doehler Jarvis test bar die on 6 June exhibit progressively serious deterioration in the runner area of the ejector die.

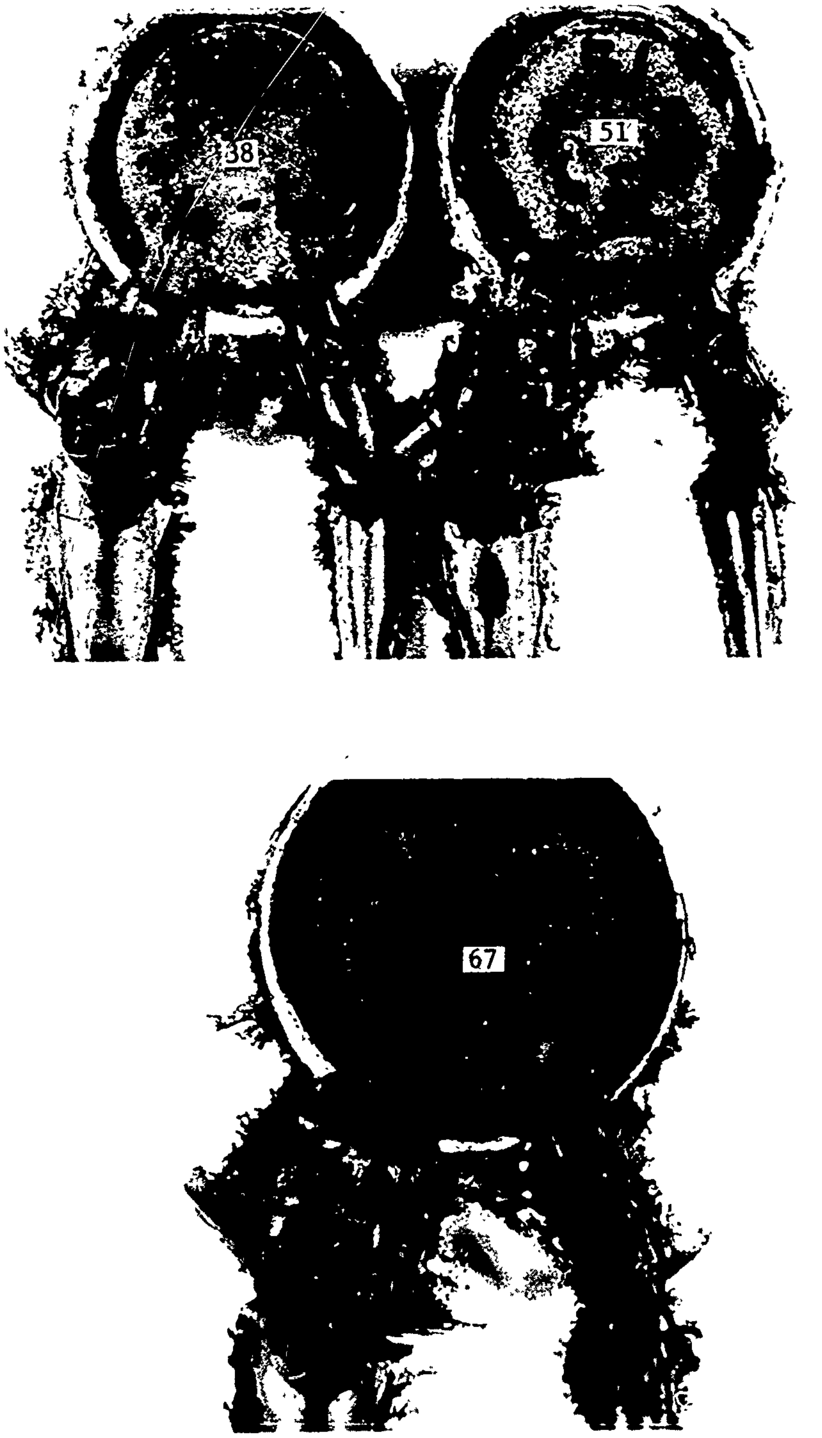


Figure 79 - Castings made in the Doehler Jarvis test bar die on 6 June exhibit progressively serious deterioration in the runner area of the ejector die.

Inasmuch as 304 stainless steel was the material being used by the Lamp Metals and Components Department in the materials evaluation phase of the subject contract, and inasmuch as 304 stainless steel is metallurgically less complex than 403 stainless steel, 304 stainless was the specific substitute recommended. On the basis of the experience reported below, it also appears that 304 stainless steel is less prone to weld to refractory metal dies than is 403 stainless steel. Both, however, present serious handling problems in the form of slag, which becomes progressively heavier and more difficult to handle with the passage of time.

Doehler Jarvis had discontinued casting on their hemisphere die because ejection had become very difficult. The source of this difficulty was the gaps which were opening at every interface between die components which was exposed to liquid metal.

As reported earlier, Doehler Jarvis proposed a self-cleaning joint to solve the gap problem. That approach was demonstrated to be effective, but it also possessed certain inherent disadvantages. First, it required unduly large and complicated impression blocks. The runners, perforce, had to be long and serpentine. The length of the runners increased the temperature drop in the metal, thus requiring higher melt temperatures and/or higher injection velocities, both of which promoted welding. The serpentine nature of the runners also contributed to turbulence in the runner system. This, too, may have promoted welding. (See Figures 78 and 79 for example.)

In view of the disadvantages of the self-cleaning joint, another solution to the gap problem was sought. The solution finally adopted by Doehler Jarvis involves machining a tapered slot or safety groove into the interface between die components. Flash is permitted to fill this groove. But, to prevent the flash from progressively opening up the bottom of the groove, it is effectively stopped by a rod recessed in the interface between the two die components or by a shoulder on one or the other of the two components. (See Figure 80.) This configuration results in a "cockscomb," encircling the runner and solidly attached to it, which does not interfere with ejection of the casting.*

With safety grooves incorporated in the hemisphere die, Doehler Jarvis resumed casting on the twentieth of June. Five shots were made with 403 stainless steel. A shot sleeve made from nitrided AISI D5 steel (1.5% carbon, 12% chromium, 1% molybdenum, 3% cobalt) was used. The hardness of the shot sleeve was Rockwell C 60. A beryllium-copper plunger with a room temperature clearance of less than 0.001" was employed. The casting conditions were as follows:

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*It was the expressed intention of the Doehler Jarvis Division of the National Lead Company to file a patent application on the "Safety Groove" design concept.

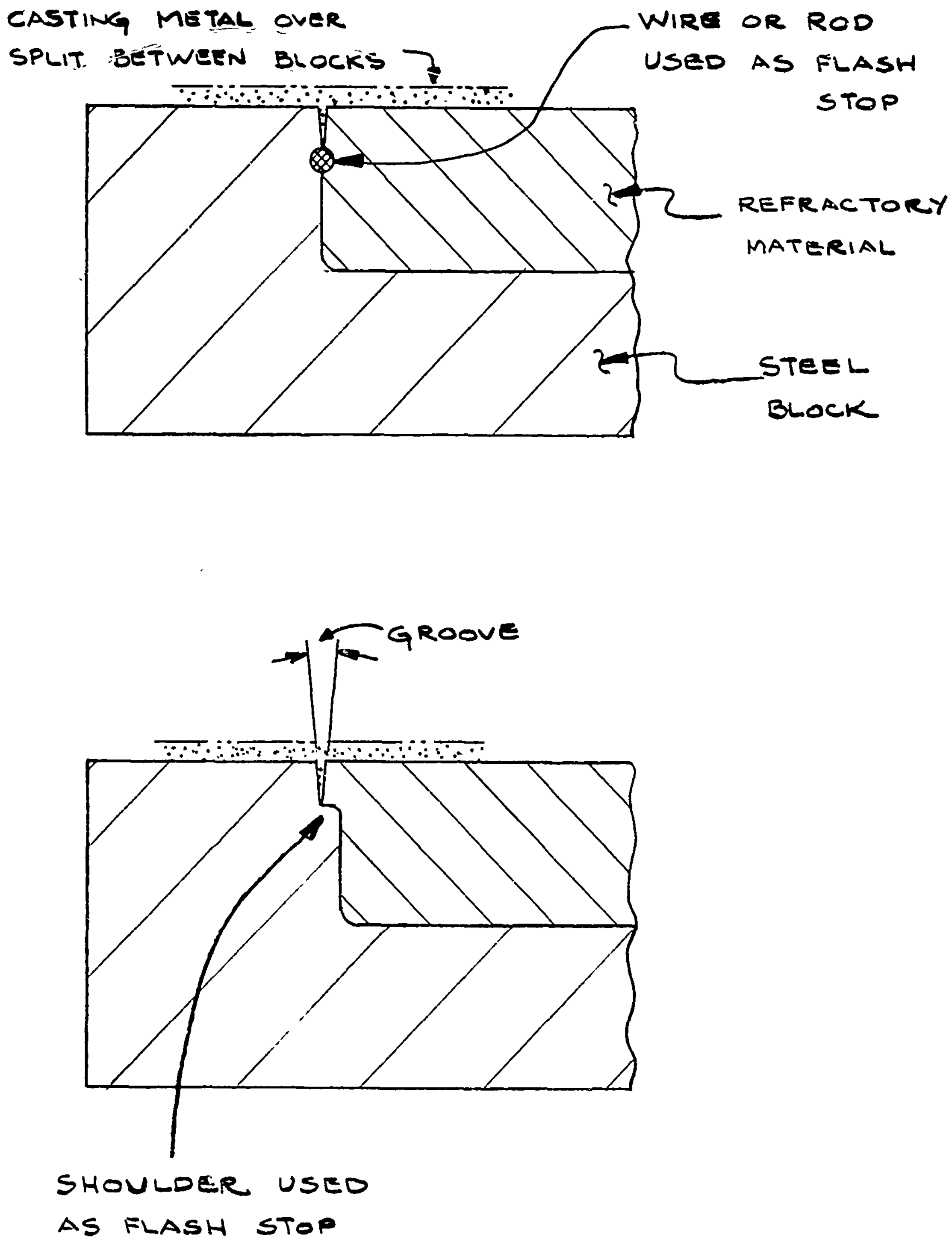

Figure 80 - Safety groove - Doehler Jarvis' solution to the problem of gap formation at interfaces between die components exposed to molten metal.

Metal Temperature	2910°F-3060°F
Plunger Speed	3-6 ips
Pressure on Metal	24,000 psi
Die Temperature	505°F-580°F
Dwell Time	4 seconds

The third shot welded in the runner area of the ejector die and had to be chiseled out. After the fifth shot, the D5 sleeve showed a longitudinal crack through its wall, approximately 4" long; and the runner was found to be welded onto the upper surface of the shot sleeve flange. Casting had to be stopped.

On the twenty-fourth of June, only one shot could be made with 403 stainless steel. The H-13 shot sleeve that had been used previously was reinstalled. The casting conditions were:

Metal Temperature	3160°F
Plunger Speed	7 ips
Pressure on Metal	4600 psi
Die Temperature	685°F
Dwell Time	4 seconds

The biscuit welded to the ejector die in the area opposite the plunger. It was removed by chiseling, and the die had to be reground.

On the twenty-fifth of June, 13 shots were made, again with 403 stainless steel, under the following conditions:

Metal Temperature	2920°F-3000°F
Plunger Speed	4-9.5 ips
Pressure on Metal	2800 and 3600 psi
Die Temperature	385°F-425°F
Dwell Time	4 seconds

No ejection difficulties were experienced on this run until the last shot, when the runner welded to the top surface of the shot sleeve flange, and the casting operation had to be stopped. Two cracks, 1" apart, were noted in the protuding lip of ejector impression Block 1 (high-density, pressed and sintered molybdenum). The lip, which forms the interlocking channel in the casting, was severed from top to bottom by the cracks. A build-up of slag in the induction furnace was also observed.

On the twenty-sixth of June, 21 shots were made with 304 stainless steel under the following conditions:

Metal Temperature	2850°F-3080°F
Plunger Speed	3.5-8 ips
Pressure on Metal	3500 and 3600 psi
Die Temperature	560°F-600°F
Dwell Time	5 seconds

Before each shot, the die was coated with Glas-Mol and acetylene soot. In addition, a coating of Arco-Perm was applied to the runner areas. After Shot 15, heavy slag built up in the induction furnace. At the same time, the stainless steel ladles became covered with a thick layer of slag, which was difficult to remove. This slag build-up and the skull formation on the ladles prompted the termination of the casting operation.

On the twenty-seventh of June, 30 shots were made with 304 stainless steel following the same die coating procedure as on the day before. The casting conditions were:

Metal Temperature	2740°F-3300°F
Plunger Speed	4 ips
Pressure on Metal	3400 and 3500 psi
Die Temperature	525°F-620°F
Dwell Time	4 seconds

Again, heavy slag build-up in the induction furnace and skull formation on the ladles stopped the casting operation.

On the twelfth of July, 56 shots were made in 304 stainless steel with the cracked D5 shot sleeve. Following the established practice, a water-cooled beryllium-copper plunger was fitted into the sleeve with a clearance of less than 0.001". The gate runner was widened to 15/16" and the in-gate thickness increased to 1/8".

A cover was constructed to fit over the melting furnace, and a protective atmosphere was maintained over the melt by introducing a flow of 30 cubic feet per hour of argon. This retarded, but did not eliminate, the slag build-up. The slag had to be mechanically removed from the furnace at intervals which became progressively shorter as the run continued.

The casting conditions were as follows:

Metal Temperature	2920°F-3030°F
Plunger Speed	1-7 ips
Pressure on Metal	3600 psi
Die Temperature	485°F-710°F
Dwell Time	4.2 seconds

At Shot 10, the protruding lip on ejector impression Block 1 (high-density, pressed and sintered molybdenum), which had cracked on the twenty-fifth of June, finally broke out after a cumulative total of 198 shots, as illustrated in Figure 81. After a cumulative total of 221 shots, a vertical crack was noted in ejector impression Insert 3 (rolled, powder metallurgy molybdenum bar). This crack is illustrated by Figure 82. At the end of the run, it was observed that the circumferential crack in cover impression Block 3 (pressed, sintered, and extruded molybdenum) had become larger and could no longer be referred to as "hairline." (See Figure 83.)

It was feared that, in addition to creating a handling problem, the excessive slag formation was indicative of the preferential oxidation of one or more of the constituents of the stainless steel. To determine the magnitude of the problem, Doehler Jarvis sampled the 304 stainless heat prepared for the die casting run performed on the twelfth of July.

Samples were taken after making the first and fifty-sixth shots. The time base of the experiment was not carefully recorded, but the time from meltdown to the first shot was estimated to be one hour, and the time from meltdown to the fifty-sixth shot was estimated to be six hours. The test results were contimated by replenishing the bath with additional melt stock during the course of the run.

Nevertheless, the results are of interest, because they reflect the changes in chemistry experienced during a real die casting run. Because the holding time was long, however, and because the number of shots made was small, the chemical changes observed are greater than they would have been for a production run. They reflect, essentially, the minimum capabilities of the process.

Table XVI compares the chemical analysis of the heat one and six hours after meltdown, with the certified analysis supplied with the melt stock by Howmet Corporation. Although carbon, manganese, silicon, and chromium were lost, the composition of the melt stayed well within the AISI specification for the duration of the run.

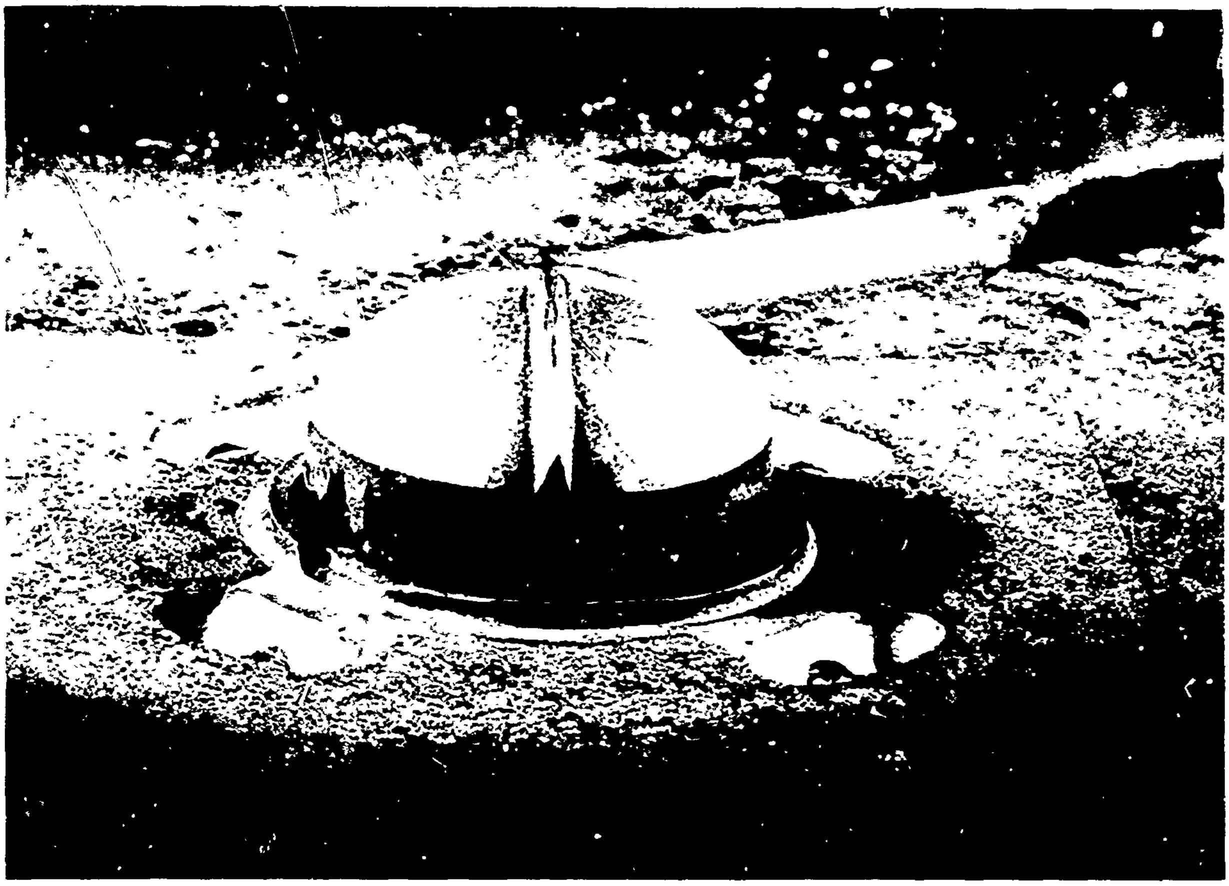

Figure 81 - High-density, pressed and sintered molybdenum ejector impression Block 1 of Doehler Jarvis' hemisphere die, with a segment of the lip broken away after a cumulative total of 198 shots.

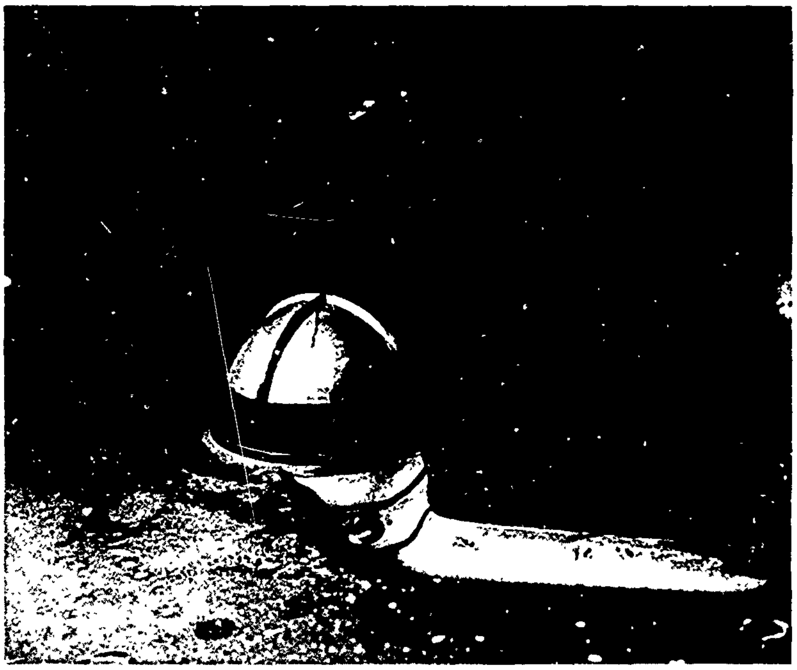

Figure 82 - Longitudinal crack noted in the wrought powder metallurgy molybdenum ejector impression Insert 3 of Doehler Jarvis' hemisphere die after a cumulative total of 221 shots.

Figure 83 - Longitudinal crack in the wrought powder metallurgy molybdenum cover impression Block 3 of Doehler Jarvis' hemisphere die after a cumulative total of 244 shots.

Table XVI--Change in Composition of a 304 Stainless Steel Melt During the Course of a Die Casting Run

Designation	Time from Meltdown	Chemical Analysis Percent						
		C	Mn	Si	Cr	Ni	Mo	Cu
Melt Stock	--	0.035	1.20	0.67	19.55	9.20	0.21	0.28
Shot 1	1 hr	0.030	1.07	0.33	19.68	9.05	0.28	0.28
Shot 56	6 hrs	0.020	0.81	0.19	18.38	9.30	0.31	0.31
AISI Standard	--	0.08 max	2.00 max	1.00 max	1800- 20.00	8.00 12.00	--	--

On the seventeenth of July, 27 shots were made from 304 stainless steel. The melt was again shielded with argon. In spite of that precaution, the slag became heavier, and its formation became more rapid as the run progressed.

A water-cooled, beryllium-copper plunger was again employed, with a clearance of less than 0.001" in a hardened, nitrided H-13 shot sleeve.

The casting parameters investigated were:

Metal Temperature	2950°F-2980°F
Plunger Speed	2-5 ips
Pressure on Metal	3600 psi and 14,400 psi (with pressure intensification)
Die Temperature	570°F-600°F
Dwell Time	4.2 seconds

On the eighteenth of July, 24 shots were made in 304 stainless steel under the following conditions:

Metal Temperature	2940°F-2960°F
Plunger Speed	3.5-5 ips
Pressure on Metal	3600 psi
Die Temperature	550°F-640°F
Dwell Time	4 seconds

In order to improve the surface quality of the castings, which was not satisfactory in the previous runs due to the heavy die coating, this run was made without acetylene black. One series of shots was made with only a Glas-Mol coating on the die; the second was made with only a coating of Aquadag* on the die.

Again, considerable slag buildup occurred in the furnace as time progressed.

On the twenty-third of July, 31 shots were made in 304 stainless steel under an argon cover. The dies were protected with a coating of Glas-Mol, followed by a colloidal graphite which was swabbed on in varying amounts.

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*Aquadag is a water suspension of graphite produced by Acheson Colloids, Port Huron, Michigan.

The range of parameters investigated is indicated below:

Metal Temperature	2920°F-2980°F
Plunger Speed	2-5 ips
Pressure on Metal	3600 psi
Die Temperature	605°F-685°F
Dwell Time	4 seconds

On the twenty-fourth of July, 29 shots were made in 304 stainless steel. The dies were again protected by the successive application of Glas-Mol and colloidal graphite. The casting conditions were:

Metal Temperature	2950°F-3050°F
Plunger Speed	4-7.5 ips
Pressure on Metal	3600 psi
Die Temperature	700°F-785°F
Dwell Time	4.6 seconds

Shots 16, 17, and 18 were made with shims between the die halves at the end of the die remote from the operator, in order to evaluate the influence of additional venting of the die. Although successively larger shims of 0.010", 0.020", and 0.030" were introduced, neither excessive flash nor spitting were observed, and the castings ejected as before.

In retrospect, this experiment begs other questions which were not considered at the time. Did the dies and platens deflect sufficiently to attain lockup in spite of the shims? Would the results have been the same with malleable iron as they were with stainless steel, or was it the viscosity of the stainless steel that prevented excessive flashing? The answers to these questions could be important considerations for die designers.

On the twenty-fifth of July, 44 shots were made from 2.80% CE malleable iron without incident. The cover die and that area of the ejector die opposite the mouth of the shot sleeve were coated with Glas-Mol. The remainder of the ejector die was coated with acetylene soot.

The casting conditions were:

Metal Temperature	2640°F-2740°F
Plunger Speed	4-7 ips
Pressure on Metal	3600 psi
Die Temperature	550°F-760°F
Dwell Time	2-4 seconds

On the twenty-sixth of July, 16 shots were made in 304 stainless steel with the same die protection scheme used on the previous run. Other casting parameters explored were:

Metal Temperature	2990°F-3040°F
Plunger Speed	3-6 ips
Pressure on Metal	3600 psi
Die Temperature	710°F-755°F
Dwell Time	2 seconds

Following that run, the dies were removed to the die room for photography. They had experienced 415 shots of molten ferrous alloys. The most notable signs of deterioration were the cracks in the two wrought molybdenum components. (See Figures 84 and 85.) Both cover impression Block 3 and ejector impression Insert 3 were pressed, sintered, and extruded, to produce a fully dense, cold-worked structure. The ejector impression insert had, in addition, been subjected to a rod rolling operation and a stress relief anneal (one hour at 950°C). The cover impression block had not been annealed following extrusion, and neither it nor the ejector impression insert were annealed after machining.

The failures were both longitudinal (i.e., parallel to the extrusion and rolling axes). More specifically, the circumferential crack in the cover impression block was not only longitudinal, but it also delineated a plane perpendicular to the short transverse direction of the rectangular extrusion. The significance of these failures is that they can be related to an anisotropy that results from cold working metals. In each of these die components, the failures were on planes perpendicular to the direction of minimum tensile ductility.

A new cover die block for Impression 2 (which had been inoperative since the failure of the tungsten-2% thoria block) was then fabricated by Doehler Jarvis from high-density, pressed and sintered molybdenum. With that impression block in place, two additional casting runs were made. Because either malleable iron or carbon steel, not stainless steel, was to be the material cast in the production and economic demonstration phases of the contract, Doehler resumed their testing with malleable iron.

On the fourteenth of August, nine shots were made in malleable iron with an H-13 steel shot sleeve and a water-cooled, beryllium-copper plunger with a clearance of less than 0.001". The operating conditions were:

Metal Temperature	2640°F-2740°F
Plunger Speed	2-4.5 ips
Pressure on Metal	4000 psi
Die Temperature	700°F-800°F
Dwell Time	1.4-3.6 seconds

Gate Side

Over-Flow Side

Figure 84 - Longitudinal crack in the wrought, powder metallurgy molybdenum cover Impression 3 of Doehler Jarvis' hemisphere die after 415 shots.

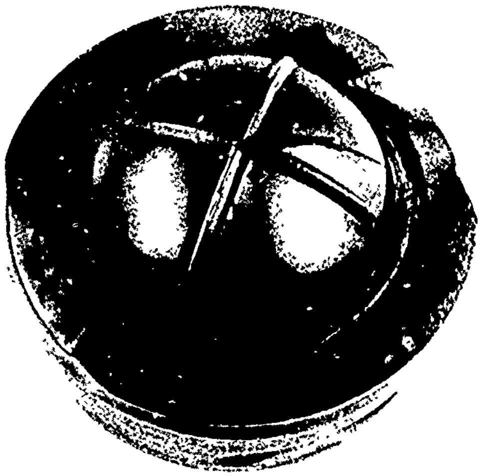


Figure 85 - Longitudinal cracks in the wrought, powder metallurgy molybdenum ejector impression Insert 3 of Doehler Jarvis' hemisphere die after 415 shots.

The second run, on the sixteenth of August, consisting of 47 shots, was again made in malleable iron under the following conditions:

Metal Temperature	2680°F-2770°F
Plunger Speed	3.5-8.5 ips
Pressure on Metal	4000 psi
Die Temperature	560°F-710°F
Dwell Time	1.4-3.8 seconds

From Shot 45 on, the biscuit started to weld in the ejector die half and had to be removed by chiseling.

Die protection in both runs was Glas-Mol 7.

An oscillographic recording was made by Doehler Jarvis for each of the 475 ferrous die casting shots made on the hemisphere die. A great number of radiographs were also made. For those interested in studying Doehler's work in depth, the data recorded oscillographically has been collected and compiled, and the results of the radiography have been classified according to the amount of internal porosity observed and compiled. The combined data is tabulated in Appendix I.

Based on their operating experience, Doehler recommended the following operating parameters to Dort for the pilot production phase of the contract:

Metal Temperature	2700°F
Gate Velocity	56 ips
Pressure on Metal	4000 psi
Die Temperature	600°F
Dwell Time	3.8 seconds,

(those recommendations being based on the assumption that Dort would produce the die cast hemispheres from 3.1% CE malleable iron of the same composition as that die cast by Doehler Jarvis).

It is also pertinent, in closing this discussion, to note that Doehler's radiographic inspection failed to reveal a single die cast hemisphere that was entirely sound, although variations in the degree of porosity were observed which could be related to the casting conditions. It was possible, on the other hand, to produce radiographically-sound 1/4" diameter reduced sections in the die cast ferrous tensile bars.

3. RESEARCH CENTER PROGRAM

Table XVII summarizes the die casting performed under this contract by the Research Center, using the Morton Vertacast Machine and the prime test bar die designed and built for the project. One measure of the success of the thermal design of that die was that those provisions made to thermally isolate the several critical components of the die enabled the Research Center to maintain the refractory metal impression blocks at temperatures above 300°C, while keeping the silicone rubber o-rings cool enough to function properly and holding the temperature of the mold base below 100°C.

The Research Center, quite naturally, also experienced some problems with their ferrous die casting operation. In addition to the feed tube difficulties, which have been described in Section II of this report, the Research Center experienced welding of castings to the molybdenum dies, breaking of castings in the dies during ejection, and unacceptable porosity in the castings.

Welding was encountered on the first cast iron run made in the test bar die. When the casting was removed from the die, a large fragment of molybdenum broke out of the radiused section at the gate end of the tensile bar cavity in the second quadrant. (The quadrant containing the flat plates is Number 4 ; as the casting is viewed from the ejector side, the quadrants, in a clockwise progression from Number 4, are numbered 1, 2, 3, in that order.) During the second cast iron run, an impact bar stuck in the die, but by exercising care, it was removed without damaging the die. By reducing the plunger velocity, the welding problem was eliminated in succeeding runs.

Breaking at the gates, before or during ejection, caused repeated difficulties. Before the seventh cast iron run on the nineteenth of August, the thickness of all of the gates was increased from 0.060" to 0.070". Thereafter, such breakage was rare.

All of the castings produced by the Research and Development Center, both in their cooperative effort with the Dort Metallurgical Company and in their own test bar die, have contained substantial porosity. As noted in Table XVII, a number of changes were made in the gate and runner system to attempt to improve the soundness of the castings produced in the Research Center's test bar die.

Table XVII--Casting Experience in the Research Center Test Bar Die

Die Changes	Date 1968	Material, Temp (°C)	Number Shots	Reason for Stopping, Remarks
1. Die installed for first time				
	20 May	Aluminum 810°C	3	Objective accomplished, die functioned properly, good filling.
	22 May	Cast Iron (3.1% CE) 1425°C	3	Tensile bar stuck to ejector die. Radiography showed 1/8" plate not filled in center. Fibrefrax/graphite/Fibrefrax feed tube okay.
2. Removed die, ground away damaged spot installed adjustable stop pads to eliminate ejector pin bosses, opened .060" gate to one plate by making 1/8" x 3/16" wide slot in center of gate				
	29 May	Cast Iron (3.1% CE)	7	Impact bar stuck to cover die, shot ram jammed on return stroke, 1/8" plates still not filling properly.
3. Removed die, removed impact bar (no die damage) opened one plate in-gate to 1/8" (full section thickness) across entire plate width				
	4 June	Cast Iron (3.1% CE) 1425°C	7	Melt froze in feed tube head. Ram sticking on return stroke.

Table XVII--Casting Experience in the Research Center Test Bar Die
(Continued)

Die Changes	Date 1968	Material, Temp (°C)	Number Shots	Reason for Stopping, Remarks
4. Opened gates to full section in ejector die in quadrant #3 (containing two impact bars and one tensile bar) by grinding in place on machine				
	11 June	Cast Iron (3.1% CE)	9	Stopped because feed tube broke. Used .010" steel cups filled from open die face for first five shots. All castings poor.
	13 June	Cast Iron (3.1% CE)	13	Emptied melt furnace. All castings good external appearance.
	25 June	Cast Iron (3.1% CE) 1425°C	7	Ram jammed on return stroke due to spill from feed tube port.
5. Die modified to provide direct feeding from central pancake. Gates increased from .060" to .070". Overfills enlarged. Shot sleeve modified to accept feed tube				
	19 Aug	Cast Iron (3.1% CE) 1425°C	11	New design feed tube. Porous alumina impregnated at surface with Ceramobond cement. Straight design, induction heated graphite susceptor. Broke on first or second shot. Replaced with Fibrefrax/graphite/Fibrefrax tube. Broke on fifth shot. Remaining castings only partially filled.

Table XVII--Casting Experience in the Research Center Test Bar Die
(Continued)

Die Changes	Date 1968-1969	Material, Temp (°C)	Number Shots	Reason for Stopping, Remarks
	26 Aug	Cast Iron (3.1% CE)	14	New design feed tube. Composite of concentric tubes of alumina/graphite/alumina. Straight through design, induction heated. Castings not filled. Plunger stuck. Feed tube broken. Replaced after Shot 5 with Fibrefrax/graphite/Fibrefrax feed tube. Broke tube in Shot 6. Made eight shots using die face feeding technique without using cup in shot sleeve. Castings poor, metal too cold.
6. Increased volume of all overflows, added 0.003" deep x 3/8" wide vents in one quadrant				
	13 Dec	Cast Iron (3.1% CE)	1	Metal penetrated annulus between plunger and shot sleeve liner. Casting stuck in cover die, plunger stuck in advanced position, Al_2O_3/graphite/Al_2O_3 induction heated transfer tube with elbow head used.
7. Central feeder made saucer shape. Slit gates opened to same cross section as cavities in one quadrant, some overflows and vents enlarged. Thickness of clamp ring reduced to produce 0.050" deep x 1" wide evacuation channel for better venting				
	6 Jan	Cast Iron (3.1% CE)	3	Plunger stuck in advanced position after second and third shots could not be retracted after third shot. Other conditions same as 13 December run.

The problem of porosity was most severe in the flat plate cavities of the test bar die. Before the second cast iron run was made on the twenty-ninth of May, a 1/8" thick by 3/16" wide slot was ground in the center of the gate leading to one of the plate cavities; little improvement was noted.

Because of the necessity of producing sound test bars, attention was next directed toward the possibility of reducing the porosity in the tensile and impact bar castings. Before the fourth cast iron run on the eleventh of June, the gates to the impressions in Quadrant 3 were ground to the full section of the cavity in the ejector half of the die only (i.e., the gate to the tensile bar cavity was half round; the gates to the impact bar cavities were triangular). No improvement was noted. Table XVIII summarizes the results of the radiographic inspection of the castings made under the same conditions on the thirteenth of June. Ironically, the castings produced in Quadrant 2, where the gates had not been modified, were, on the average, somewhat less porous than those produced in Quadrant 3, where the gates had been enlarged; but the castings produced in Quadrant 1, where the gates had also not been modified, were much more porous than those from either Quadrant 2 or 3.

The notation in Table XVIII requires some explanation. For the tensile bars, "sound" means no defects observed in the radiograph of the reduced section; "marginal" means one or two small defects noted. No impact bars were completely sound. The designation "sound" or "marginal," when applied to the impact bars, refers to the region of the notch only.

Photographic prints of radiographs did not prove satisfactory for reproduction. To provide additional insight into the degree of porosity observed in the radiographs, therefore, pen-and-ink drawings were prepared that represent, as fairly as possible, the relative number, size, shape, and distribution of the voids. No attempt was made to represent density differences--the various gradations of gray to black. Those areas indicated to contain voids have been inked in completely.

On the other end of the scale, no attempt was made to indicate gate areas by shading, but their location is readily apparent from the geometry.

Two castings were selected from the 3.1% CE malleable iron run made in the Research Center test bar die on the thirteenth of June for representation by pen-and-ink drawings. Figure 86 represents Shot 5, which produced one of the poorer castings; Figure 87 represents Shot 8, which produced one of the better castings.

Table XVIII--Summary of the Radiographic Inspection of the 3.1% CE Malleable Iron Castings Produced in the Research Center Test Bar Die on 13 June

	1st Quadrant			2nd Quadrant			3rd Quadrant		
	I	T	I	I	T	I	I	T	I
Shot No.	1-1	1-2	1-3	2-1	2-2	2-3	3-1	3-2	3-3
1	--	--	--	X	M	--	--	S	--
2	--	--	--	X	--	X	X	--	--
3	--	--	--	--	M	--	X	S	X
4	--	--	--	X	--	X	X	M	--
5	--	--	--	--	--	--	--	M	--
6	--	--	X	--	S	X	--	--	--
7	X	--	--	S	--	--	--	M	--
8	X	--	X	--	S	--	X	S	--
9	X	--	--	X	S	--	--	--	--
10	Retained for Display								
11	--	--	--	X	M	X	--	S	X
12	X	--	X	X	M	X	--	S	--
13	Poorly Filled								

Code:

I = Impact Bar Casting
M = Marginal
S = Sound

T = Tensile Bar Casting
X = Sound Near Notch
n-m = Quandrant-Cavity

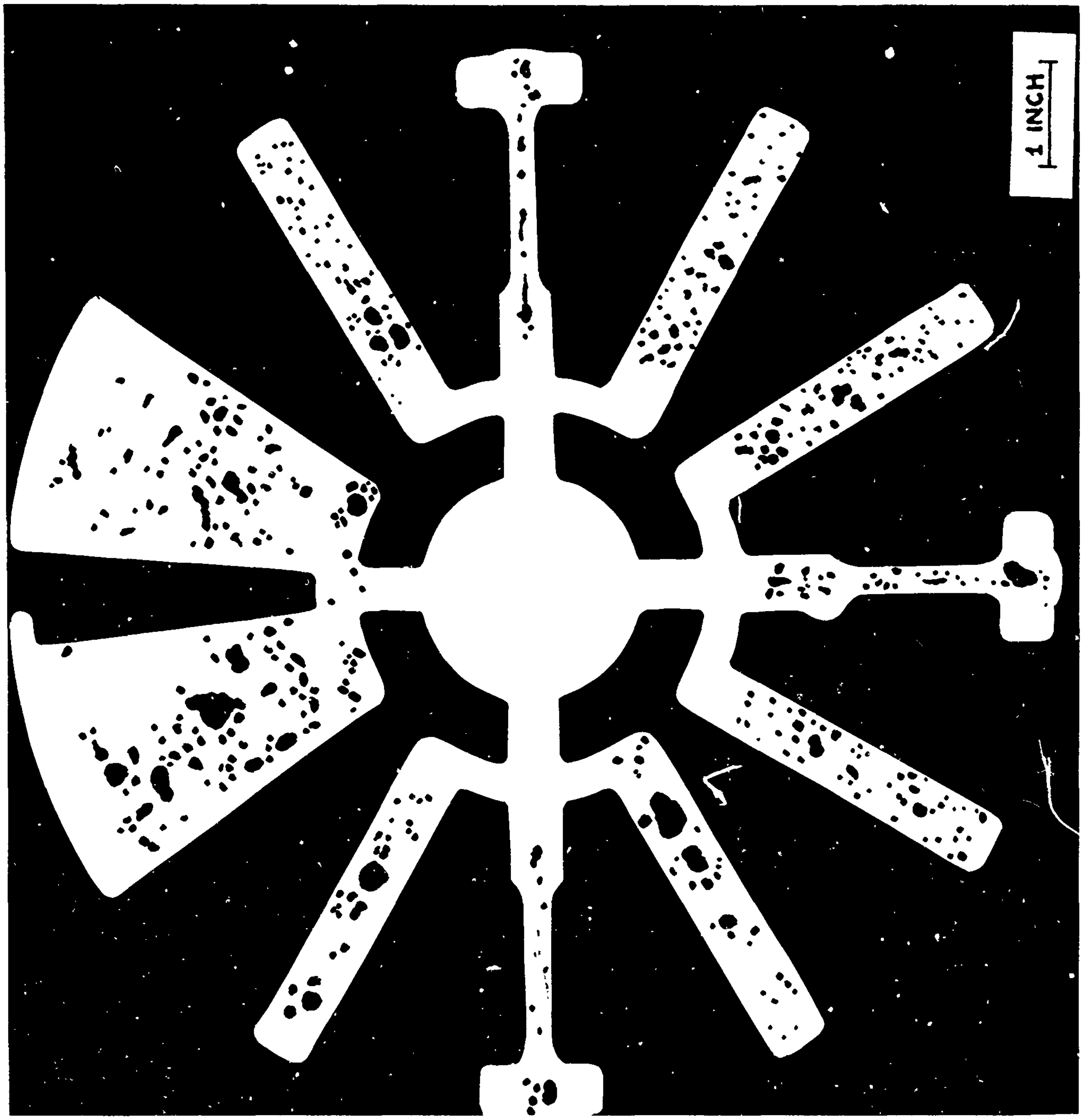


Figure 86 - Pen-and-ink representation of the porosity revealed radiographically in Shot 5; one of the poorer castings produced in the Research Center test bar die on 13 June.

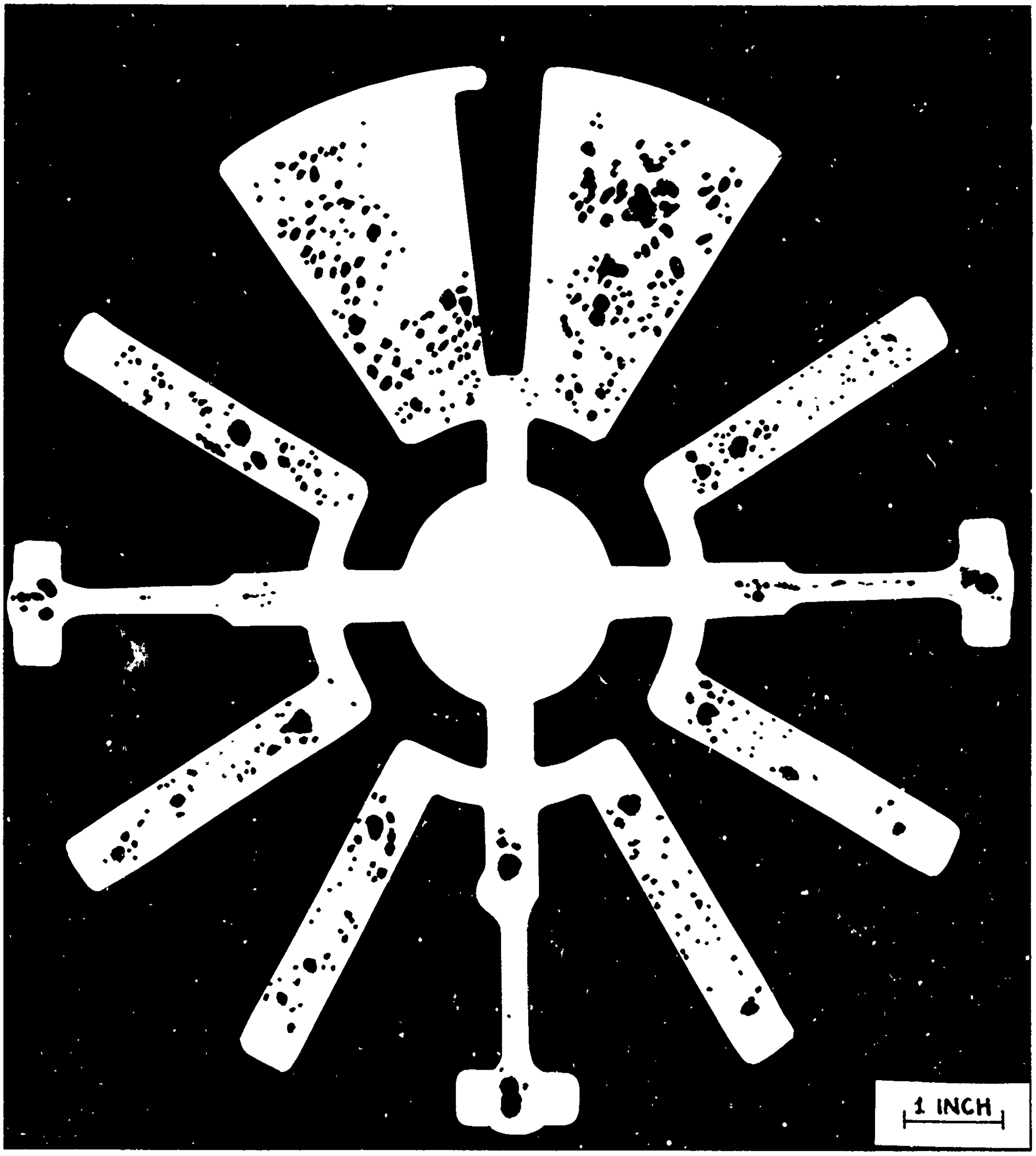


Figure 87 - Pen-and-ink representation of the porosity revealed radiographically in Shot 8; one of the better castings produced in the Research Center test bar die on 13 June.

Two additional modifications were made to the Research Center test bar die before the seventh cast iron run on the nineteenth of August. The directional changes imposed upon the metal flow by the original runner system were eliminated by cutting a disk-shape feeder into the center of the ejector die. This modification permitted the impressions to be gated directly to the central feeder. At the same time, the thickness of the gates was increased from 0.060" to 0.070". The soundness of the castings was not noticeably improved.

The persistence of the porosity produced the intuitive impression that air entrapment was the major source of difficulty. Acting on that assumption, the volume of all of the overflows was increased, and 0.003" deep by 3/8" wide vents were added to all of the cavities in one quadrant of the die.

A casting trial was made on the thirteenth of December using 3.1% CE iron and an induction-heated alumina/graphite/alumina transfer tube with an elbow-type head. Only one shot was made. The casting stuck in the cover die, and the plunger stuck in the forward position. While attempting to free the plunger by force, the shot sleeve liner was observed to move slightly. After removing the shot sleeve and the plunger, they were separated by using an arbor press. Metal had penetrated between the plunger and the shot sleeve liner. Radiography of the one casting indicated that the die modifications had apparently had little effect.

Additional changes were made to the die at that point, in an attempt to reduce porosity. For the 13 December run, the gating arrangement had been as follows: One quadrant (containing one tensile bar and two impact bars) was half-gated, i.e., fed directly from the 1/4" thick, pancake-like central feeder with the complete ejector half open; the other two similar quadrants were slit-gated with 0.070" thick openings across the entire section width; the fourth quadrant contained two 1/8" thick plates, one with full-section gates and the other with 0.070" slit gates. After the 13 December run, all of the gates in one of the quadrants that had previously been slit-gated were opened to the full cross section of the respective cavity and gated directly from the central feeder. One test bar quadrant was thereby provided with full-gates, one with half-gates, and one with slit gates. In addition, some overflows were enlarged still more, and additional vents were added to selected test bar cavities. The central feeder was modified to have a saucer-like shape. The changes were arranged to provide permutations of gate type, overfill type, and vent type. To make venting more effective, the clamp rings, which encircled each die half, were reduced in thickness to provide a clear evacuation channel (about 0.050" deep by 1" wide), through which the vents could communicate to the vacuum pump. Figures 88 through 90 illustrate the successive modifications made to the Research and Development Center's test bar die in pursuit of sound castings.

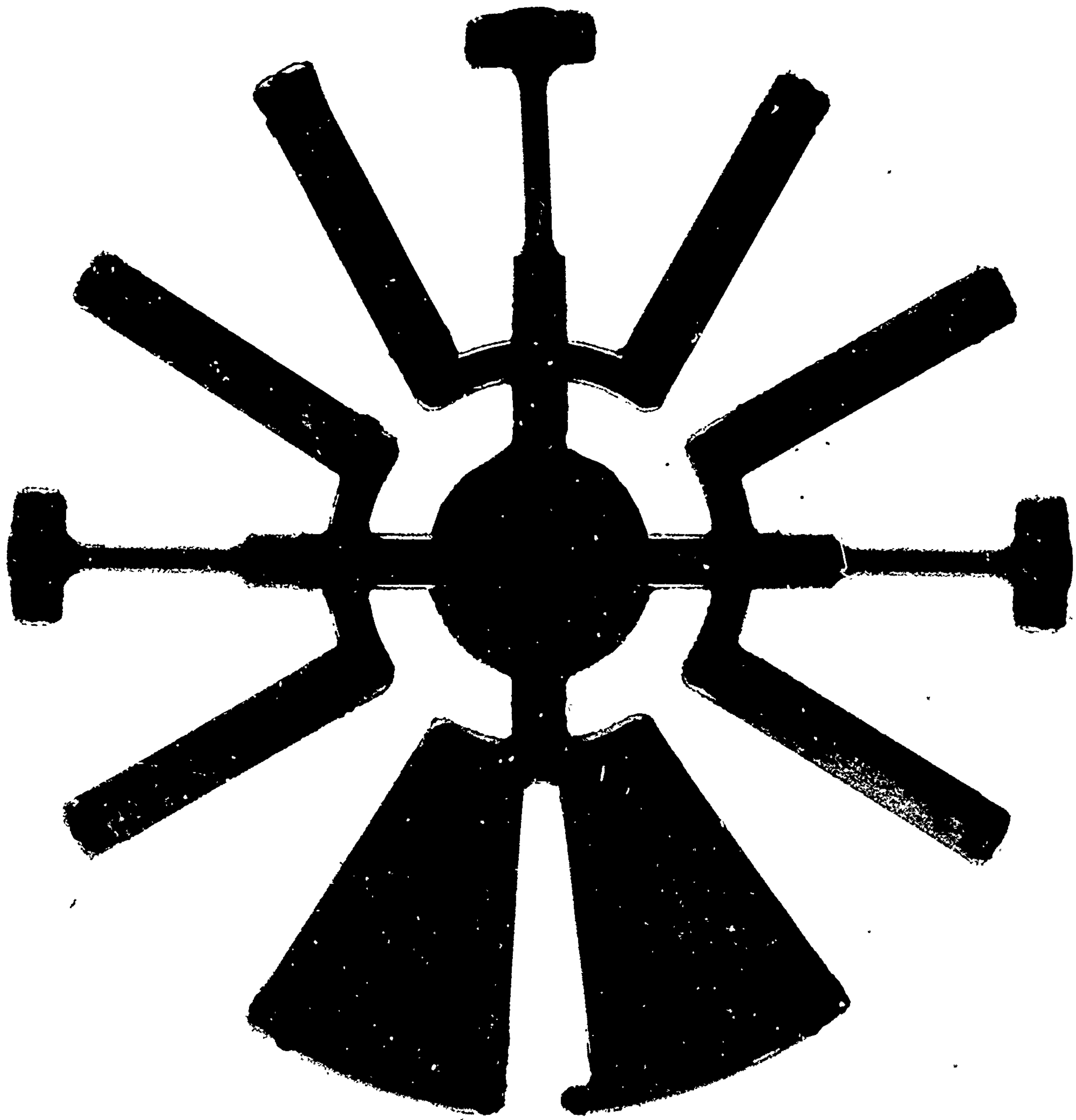


Figure 88 - Original gating arrangement in the Research Center's test bar die, represented by an RTV* silicone rubber casting made before putting the die into service.

*RTV is a product of the General Electric Company.

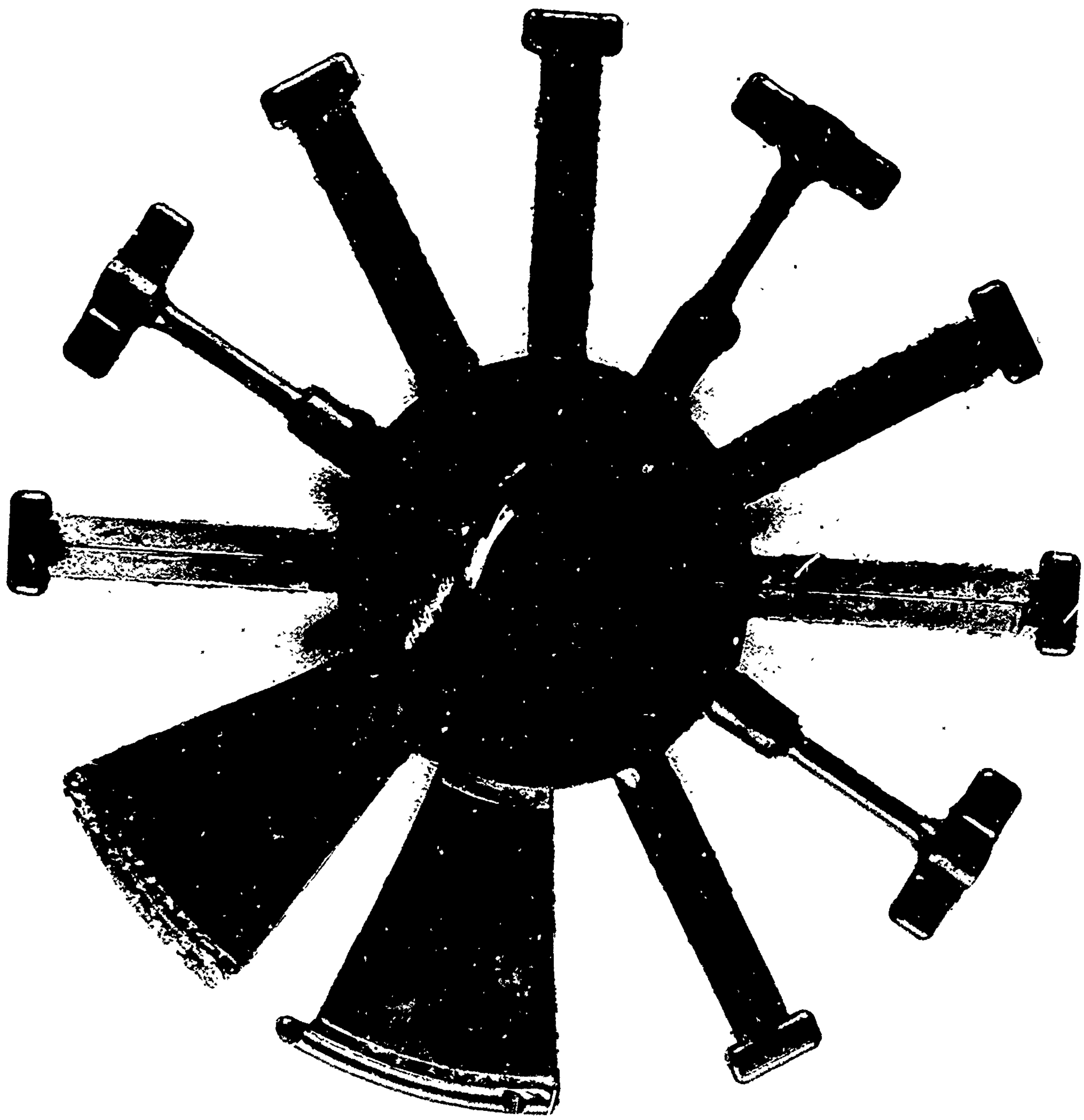


Figure 89 - Gating arrangement in the Research Center's test bar die for the 19 August run, represented by the first 3.1% CE iron casting made on 26 August.

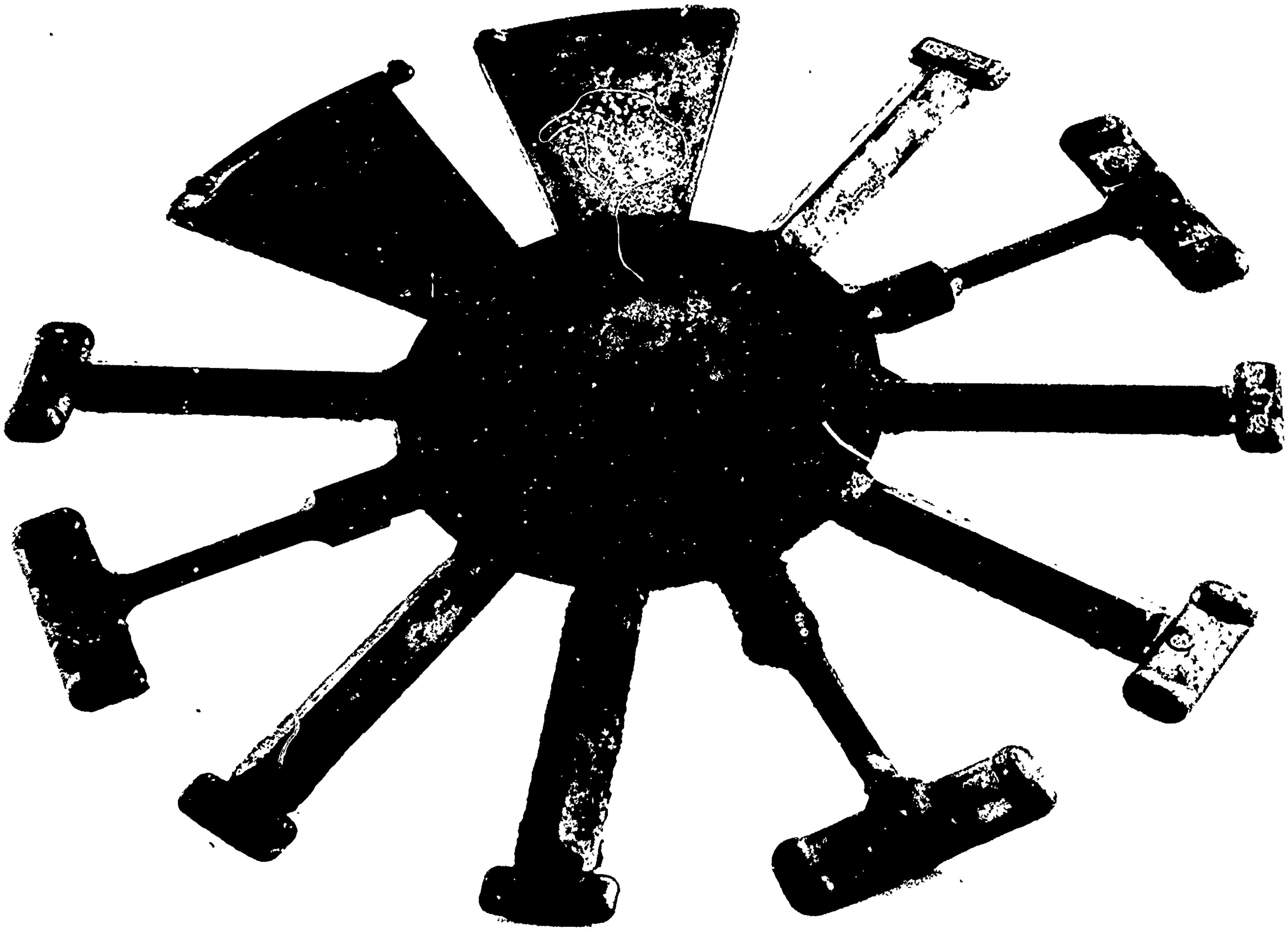


Figure 90 - Gating arrangement in the Research Center's test bar die for the 6 January run.

A casting trial was made on the sixth of January 1969. Three shots were made from 3.1% CE iron. All filled satisfactorily. The ram stuck in the advanced position on both the second and third shots; it could not be moved after the third shot.

The second casting revealed that a crack had developed in the high-density, pressed and sintered molybdenum ejector impression block, running completely across the central feeder from one ejector pin hole to another diametrically opposite. The first casting die not reveal the crack; the second did. (See Figure 90.)

Radiography of the castings from this run showed that the several permutations in the configuration of gates, overflows, and vents produced minor variations in porosity, but major porosity was still present.

Despite several attempts, the Research Center was unable to devise a satisfactory technique for reproducing the information on radiographic films. In general, porosity is more prevalent in larger sections. (The 1/2" diameter ends of tensile specimens have more porosity than the 1/4" gage sections.) The type of gate exerts substantial influence. Slit-type gates produce many small, generally distributed pores, while half-gates and full-gates produce fewer but larger pores. Some tensile specimens with half-gates and full-gates are almost free of porosity in the reduced section. (Exceptions appear sporadically to refute such generalization, suggesting that other uncontrolled variables also have an influence.) The Research Center's inability to sustain operation long enough to explore and control machine variables has undoubtedly clouded the results.

Radiographs were normally made on complete gates with the film parallel to and the X-ray beam perpendicular to the parting plane. To provide a different perspective, one gate from the run, made on the sixth of January, was sectioned and photographed. (See Figure 91.) In this photograph, each test bar casting was sectioned perpendicular to the parting plane and spread open in its respective location around the central feeder. The adjacent edges of a particular specimen correspond to the bottom edge of the casting in the die. The several gate configurations are evident. One of the 1/8" thick plates was surface ground to its mid-plane, parallel to the parting plane. The porosity patterns are clearly evident.

The pores are distinctly rounded, suggesting entrapped gas, rather than solidification shrinkage. There is a marked tendency for the pores to be in the upper half of the specimen, suggesting flotation. There is much more porosity in the thicker (last to freeze) sections than in the thinner sections. The porosity is greatest and more generally distributed in the slit-gated specimens. The full-gated tensile specimen contains porosity only in the enlarged ends, and there is more porosity in the end away from the gate. Within the central feeder and biscuit, the metal is generally free from porosity, except for a rather large central void, presumably representing solidification shrinkage.

Figure 91 - Indication of the porosity in the 3.1% CE iron castings produced in the Research Center's test bar die on 6 January--revealed by sectioning. (See text for details.)

One of the important contributions which the Research and Development Center made to ferrous die casting was achieved using General Electric Company funds exclusively. Nevertheless, it is reported here because of its importance, both to the quality of the product and to the economics of the process (as will be seen in Section IX of this report). That contribution was the discovery of a technique for continuously and accurately monitoring and controlling the melt temperature.

The technical significance of the achievement can easily be underestimated, if one overlooks the many facets of the die casting process that are affected directly by melt temperature. Below is an abbreviated list of the many aspects of the process and product which are or may be related to melt temperature:

a. Final dimensions or parts and dimensional variability

b. Solidification shrinkage in parts

c. Hot tearing of parts

d. Surface quality of parts

e. Propensity to solder the die

f. Die life

g. Crucible life

h. Ease of maintaining melt composition

i. Dwell time -- cycle time.

The Research Center concluded that intermittent radiation pyrometer measurements were neither convenient nor sufficiently accurate. Continuous readings from platinum/platinum-rhodium thermocouples were desired, providing a suitable protection sleeve could be obtained. Dense alumina protection tubes shattered, due to thermal shock. Thin-walled zircon protection tubes did not crack, providing they were preheated by lowering them slowly through the hot zone above the melt prior to immersion. Experience showed, however, that the silicon contained in the zircon contaminated the thermocouples rapidly, leading to erroneous results.

Nevertheless, a workable system was finally devised which could be used repeatedly and for exposure times of several hours. An outer, closed-end protection tube of zircon was used over an inner, closed-end protection

tube of dense alumina, inside which was inserted the platinum/platinum-rhodium thermocouple wire insulated with conventional, double-bore alumina beads. The zircon tubes employed had a 1/2" outer diameter by a 1/16" wall and were fabricated by the Ceramics Unit of the Research and Development Center. It was the zircon that provided the thermal shock protection for the alumina. (Mullite is another possibility for this application.) The alumina inner tubes, which were purchased from an outside source, had an outside diameter of 5/16" by a 1/16" wall. The alumina inner tube protected the thermocouple from contamination with silicon from the zircon. The relatively thin walls of the protection tubes and the relatively high thermal conductivity of the alumina at elevated temperatures minimized thermal lag, so that the effect of slight adjustments in power were evident in less than one minute. This system was used by the Research Center with cast iron and other high melting alloys with satisfaction on numerous occasions. Occasionally, defective tubes required replacement, but the tubes are inexpensive and easily replaced during operation.

Section VI will deal with the structures and properties of die cast materials. One of the observations made by the Research Center as a result of their participation in that effort was that the properties of the AISI 4340 die cast by Dort were neither characteristic of that alloy nor internally consistent. Analysis indicated that the 4340 melt had undergone a change of composition during the course of the casting run.

As a result of their experience with the AISI 4340, the Research Center made an effort to determine the effect of holding time on the composition of melts of the following alloys:

a. AISI 4340 steel

b. AISI 403 stainless steel

c. 3.1% CE malleable iron.

The AISI 4340 was air melted from a scrap charge by the Dort Metallurgical Company. Chemical analysis indicated that the levels of the elements chromium, molybdenum, nickel, sulphur, phosphorus, aluminum, and copper did not change significantly during the course of the run. The levels of the elements carbon, manganese, and silicon, however, did undergo a marked change with time, as indicated in Table XIX, which presents composition as a function of time after pouring sand cast test bars as a prelude to the die casting run.

Table XIX--Effect of Holding Time on the Carbon Manganese, and Silicon Levels of an AISI 4340 Air Melt

Elapsed Time (Minutes)	Carbon (%)	Manganese (%)	Silicon (%)
Standard	0.38-0.43	0.60-0.80	0.20-0.25
19	0.29	0.21	0.03
40	0.19	0.14	0.04
58	0.09	0.11	0.02
68	0.05	0.13	0.09

Carbon Loss: Approximately 0.005% per minute

Table XX presents similar data for air melted 403 stainless steel. No significant compositional changes occurred during the course of the run. The initial chromium content of the melt, however, was significantly lower than the standard composition. The initial carbon level, on the other hand, was significantly higher than the standard composition. Although the chromium may have been lost during the melt-down and holding periods, prior to the beginning of the run, the coincidentally high carbon level lends credence to the suspicion that the scrap charge was off composition.

Defining the carbon equivalent (CE) as the percentage of carbon plus one-third of the percentage of silicon, it is clear that both of the nominally 3.1% CE heats analyzed for the preparation of Table XXI were significantly off chemistry. Such deviations will affect many of the properties of the cast iron, but the most significant effect will be the effect upon the response to heat treatment. Lower than anticipated levels of carbon and silicon will markedly increase the time required to malleablize a cast iron. In these data, and in the data for the 403 stainless, there is a clear warning for those who would base a ferrous die casting operation on purchased scrap.

The effect of holding times on melt compositions, too, will be important to the ferrous die caster. Heine[15,16] has studied this problem for cast irons. He found that silicon and manganese tend to be lost by oxidation at lower temperatures. Carbon contents, on the other hand, can be expected to remain stable for periods of one to two hours just above the liquidus. At higher temperatures, the loss of silicon and manganese by oxidation is reduced, but the rate of loss of carbon is increased. In fact, at temperatures corresponding to 300°F to 600°F superheat, the carbon loss may be in the order of 0.01% to 0.02% per minute. (At high temperatures, evaporative losses of such volatile elements as manganese and chromium may also become significant.)

For the ferrous die caster, of course, the loss of carbon, which has such a profound effect on the properties of ferrous alloys, will be most troublesome. Returning to the data in Tables XIX through XXI, it will be observed that carbon was lost from the AISI 4340 melt at a slightly higher rate than from the malleable iron melt. This is somewhat surprising, considering the fact that the carbon level in the malleable iron is much higher than the carbon level in the 4340. The explanation, however, is the difference in melt temperatures. Whereas the malleable iron was successfully die cast at melt temperatures as low as 2480°F, and was never cast at temperatures higher than 2700°F, the 4340 was cast at 2900°F to 2940°F. The 403 stainless steel, on the other hand, which

Table XX--Effect of Holding Time on the Carbon and Chromium Levels of an Air Melt of 403 Stainless Steel

Elapsed Time (Minutes)	Carbon (%)	Chromium (%)
Standard	0.15 max.	11.5-13.0
2	0.27	9.8
12	0.26	9.8
24	0.25	9.5

Table XXI--Effect of Holding Time on the Carbon, Manganese, and Silicon Levels of Melts of 3.1% CE Malleable Iron in Air and Under Argon

Elapsed Time (Minutes)	Carbon (%)	Manganese (%)	Silicon (%)
Air Melted by Dort Metallurgical			
7	2.40	1.39	0.46
21	2.33	1.40	0.42
48	2.24	1.32	0.48

Carbon Loss: Approximately 0.004% per minute

Elapsed Time (Minutes)	Carbon (%)	Manganese (%)	Silicon (%)
Melted Under Argon by the Research and Development Center			
0	2.51	1.75	0.57
6	2.49	1.78	0.57
12	2.46	1.73	0.57

Carbon Loss: Approximately 0.004% per minute

was also cast at a melt temperature of 2900°F, appeared to have lost very little of its carbon. To explain this anomaly, it may be hypothesized that the relatively high chromium content of the 403 stainless steel reduced the carbon activity of the melt. In any case, the significance of these data is that carbon depletion will be most pronounced in plain carbon and low-alloy steels where it can be tolerated least.

The argon cover over the malleable iron heat melted by the Research Center was ineffective. Although elaborations of this approach can certainly be made successful, its use may be restricted by economic realities. Another approach, that is more appealing for an operation employing hand ladling, is to minimize the holding time by maintaining high production rates and by limiting the size of the melt to that amount that can be cast in 20 to 40 minutes.

SECTION VI

STRUCTURES AND PROPERTIES OF DIE CAST FERROUS ALLOYS

In the final analysis, ferrous die casting will be evaluated by consumers and suppliers who will evaluate the products manufactured by the process. The criterion of paramount importance will be cost of function. One of the many factors having an important bearing on the cost of function is the mechanical properties of the product, which are inextricably related to the microstructures of the materials from which the products are made. Also, in the metallurgically complex system of ferrous alloys, it is not only the as-cast microstructures and properties that are important, but also those that can be attained by subsequent heat treatment.

Within the monetary and temporal limitations of the contract, the microstructures and properties of the following die cast materials were explored in varying detail:

a. Eutectic iron (4.25% CE)

b. Nodular "ductile" iron

c. Malleable iron with carbon equivalents of 3.78%, 3.1%, and 2.6%

d. AISI 4340 steel

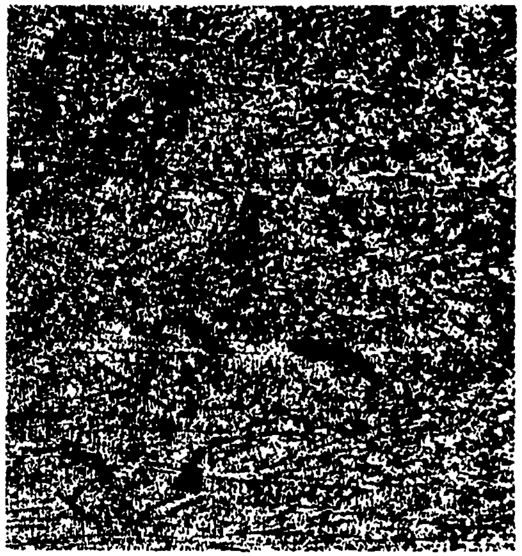
e. Two grades of stainless steel, AISI 403 and AISI 304.

1. EUTECTIC IRON

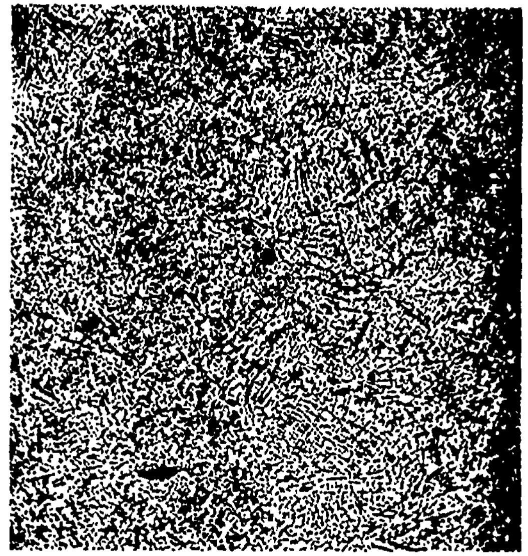
Before this contract was signed, the Research and Development Center had die cast a number of 4.25% CE (eutectic) iron step plates into high-density, pressed and sintered molybdenum dies. The composition of the iron was 3.41% carbon, 2.52% silicon, 0.51% manganese, 0.075% sulfur, and 0.15% phosphorus; the melt was prepared from scrap grey iron castings; the pouring temperature was 1350°C (2462°F). The step plates were approximately 1" wide x 3" long, with steps of 1/32", 1/16", and 1/8". The die temperature was maintained at 400°C (752°F).

All sections solidified as white cast iron, without graphite formation. Secondary dendrite spacings were very small. The spacings were about two microns in the 1/8" sections, and were progressively smaller in the 1/16" and 1/32" sections, as illustrated by Figures 92 and 93.

1/8" Section
Avg Hardness = 837 DPH

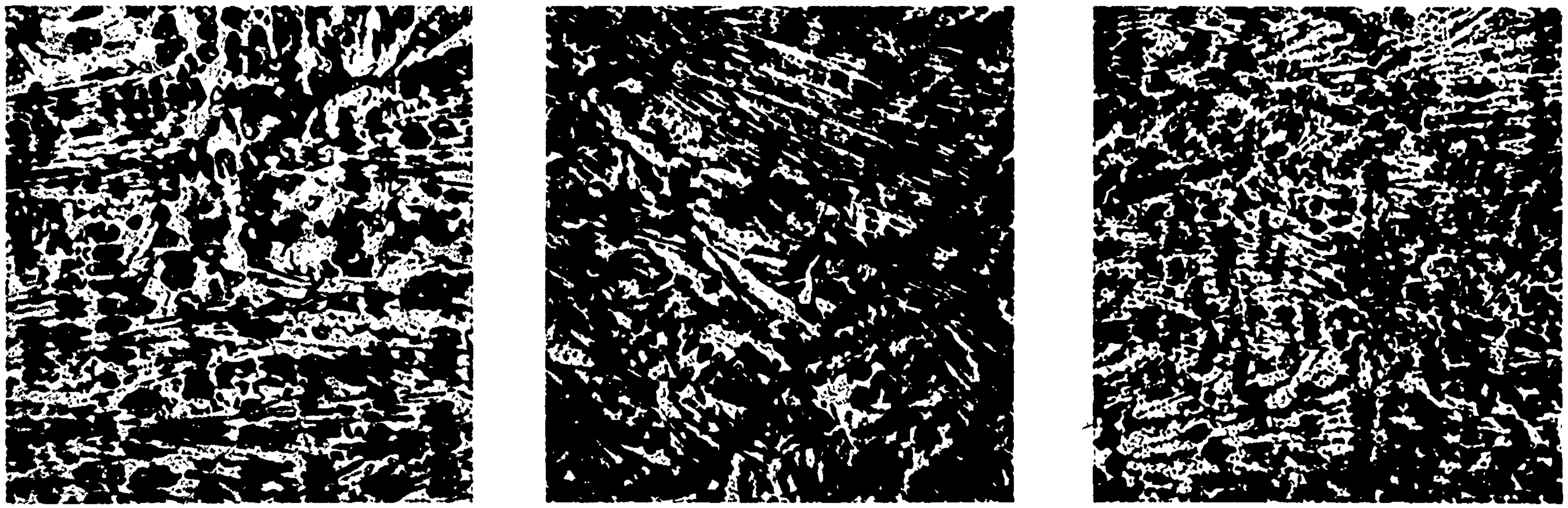


1/16" Section
Avg Hardness = 929 DPH



1/32" Section
Avg Hardness = 1022 DPH

Figure 92 - Microstructure of 4.25% CE iron as die cast, 100X magnification.



1/8" Section
Avg Hardness = 837 DPH

1/16" Section
Avg Hardness = 929 DPH

1/32" Section
Avg Hardness = 1022 DPH

<u>Figure 93</u> - Microstructure of 4.25% CE iron as die cast, 1000X magnification.

The white dendrites are iron carbide, and the darker areas are the iron/iron-carbon eutectic and pearlite, both of which were resolved optically at 1000X. (Much of that resolution has been lost in the process of reproduction.) In their cooperative program with the Research Center, Dort die cast and sand cast samples of 4.2% CE iron. The microstructures observed in those castings are presented for comparison in Figures 94 and 95, respectively.

Inasmuch as the 4.25% CE die cast iron contained no free graphite, the Research Center undertook a study of the graphitization of the material by annealing. The results for the castings with a 1/32" thickness are represented graphically by Figure 96.

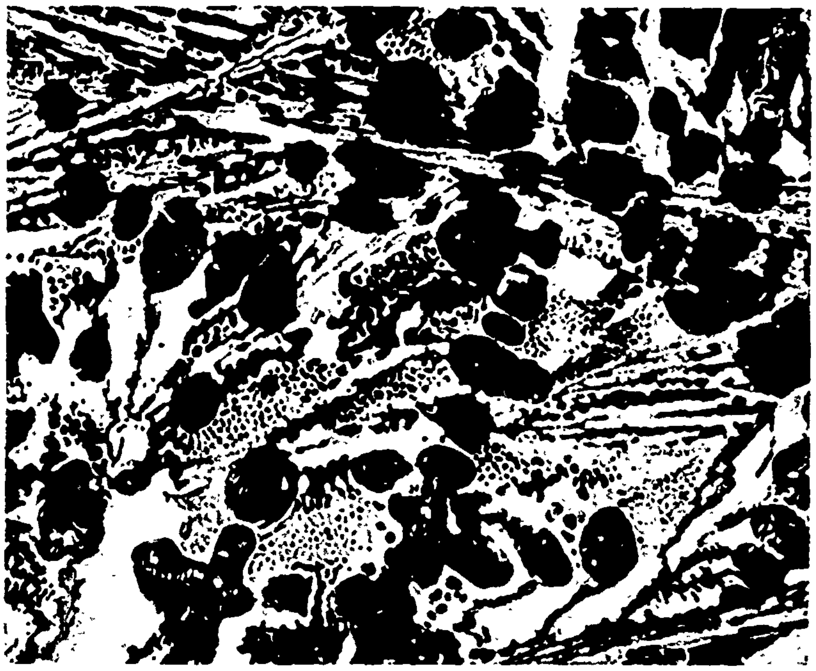
Time-temperature-hardness surfaces for the 1/16" and 1/8" section sizes are similar to the surface generated in Figure 96 for the 1/32" section size, but there are some points of difference. The as-cast hardness is lower for the greater section thicknesses. Short-time, high-temperature heat treatments, e.g., 15 to 30 minutes at 900°C (1652°F), tended to develop the same hardness in all three section sizes. Longer times at high temperatures, e.g., one to two hours at 900°C (1652°F), tended to soften the thick sections more than the thin sections. These trends may be clearly discerned from the hardness data tabulated in Table XXII, and are illustrated by Figures 97 and 98.

Low temperatures, i.e., $<700°C$ (1292°F), were investigated to determine if the cast structures, which were assumed to be highly non-equilibrium, could be graphitized in short times. Such a short graphitization treatment would make it possible to eject parts from the die directly onto the belt of a continuous annealing furnace. Although some graphite was produced at low temperatures, the matrix invariably contained large quantities of undecomposed, primary carbide. Even after annealing at 700°C, considerable primary carbide remained undecomposed in specimens of all three section sizes; and only the 1/32" specimen was softened to a hardness level approaching that of ferrite. (See Figure 99.)

None of the samples die cast from eutectic iron, including those produced by Doehler Jarvis, Dort, and the Research Center, were satisfactory for tensile testing. Tensile test results, therefore, are not available for the 4.25% CE iron.

2. NODULAR IRON

The purpose of this cursory investigation, performed by the Lamp Metals and Components Department, was simply to indicate the feasibility of producing nodular iron in the die casting operation.



Mag. = 1000X

Figure 94 - Microstructure of 4.2% CE iron, as die cast by Dort in cooperation with the Research Center. Average hardness = 639 DPH, 51.4 R_C

Mag. = 100X

Mag. = 500X

Figure 95 - Microstructure of a 0.505" diameter tensile bar, sand cast from 4.2% CE iron.

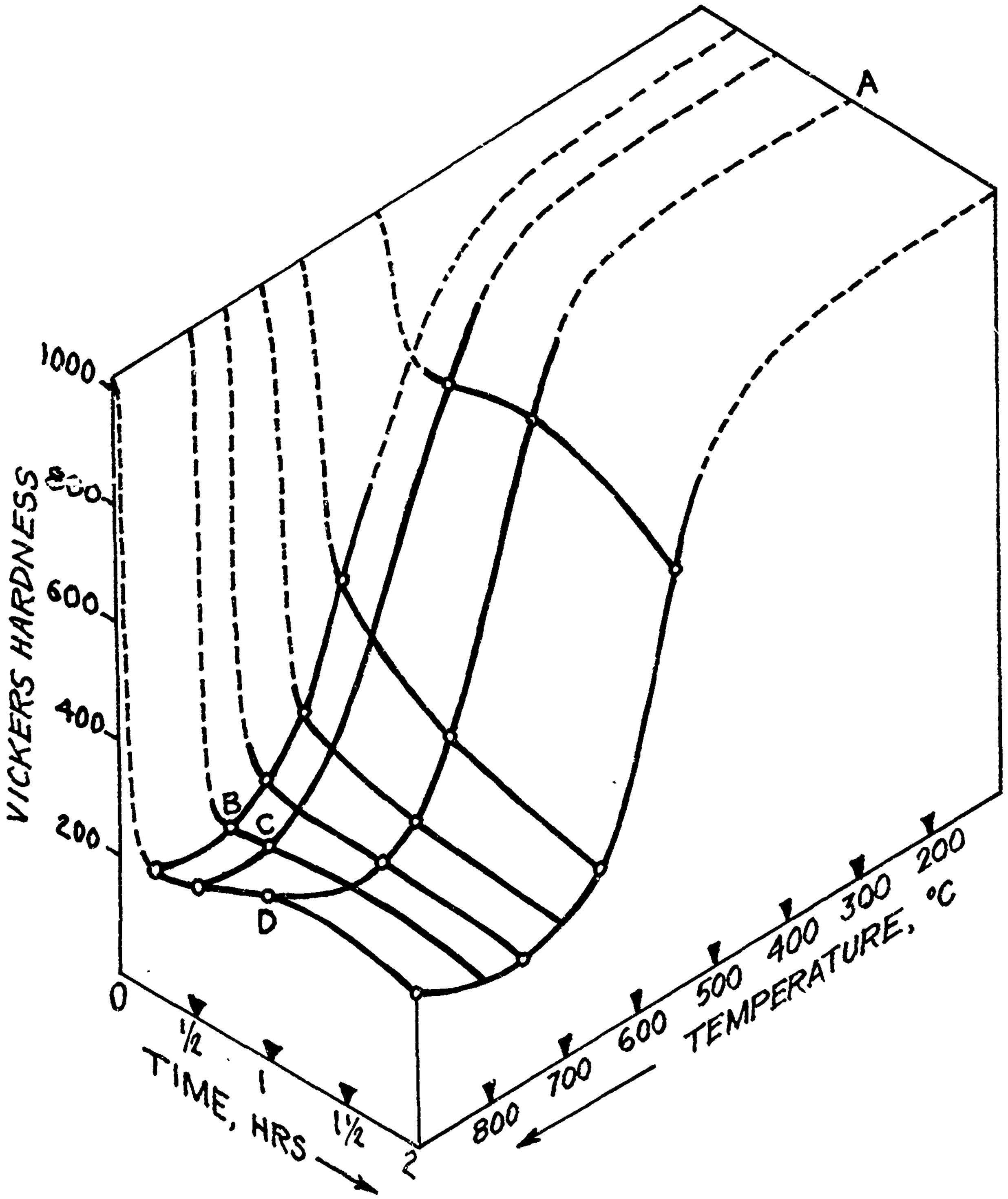
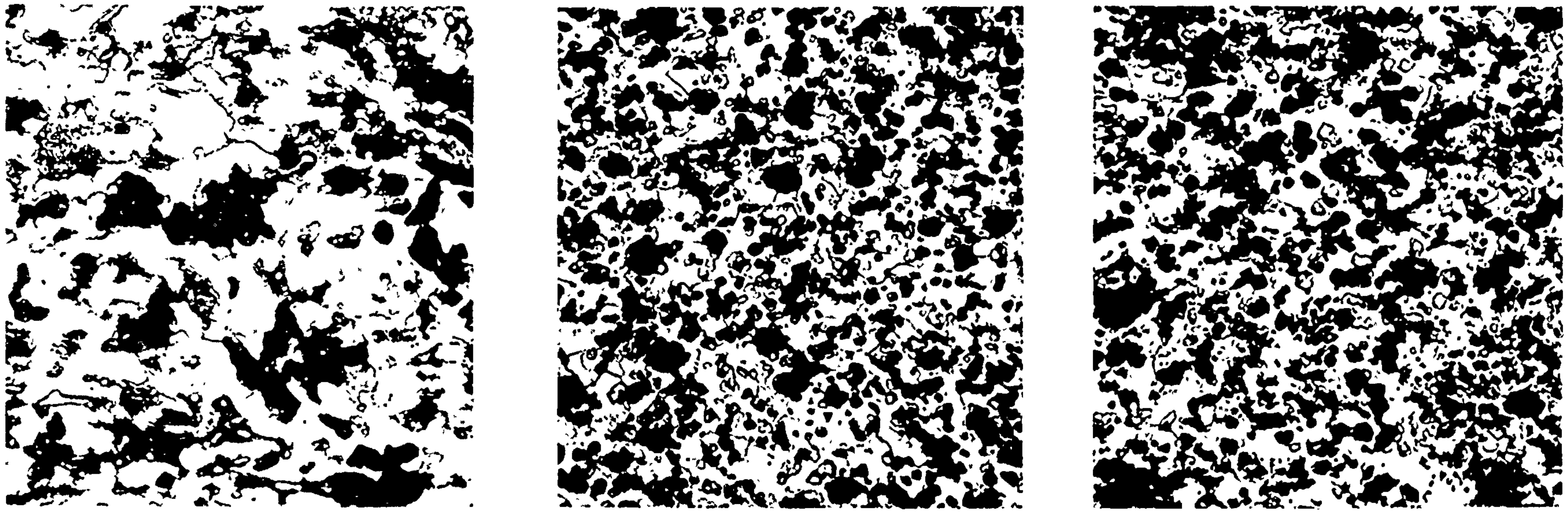


Figure 96 - Time-temperature-hardness surface for heat-treated, die cast 4.25% CE iron (section size = 1/32").

Table XXII--Vickers Hardness of 4.25% CE Iron, Die Cast and Annealed by the Research and Development Center

Heat Treatment	Vickers Hardness (Kg/mm^2) 1/8" Section 1	2	3	Avg	1/16" Section 1	2	3	Avg	1/32" Section 1	2	3	Avg
As die cast	824	864	824	837	960	920	907	929	1040	1003	894*	1022
900°C, 15 min, air cool	222	232	242	232	222	225	227	225	222	221	225	223
900°C, 30 min, air cool	251	234	237	241	240	243	244	242	266	256	254	259
900°C, 1 hour, air cool	249	261	270	260	284	279	285	283	278	283	216*	280
900°C, 2 hour, air cool	232	232	235	233	238	243	243	241	274	254	264	264
800°C, 15 min, air cool	256	247	252	252	236	243	235	238	205	226	151*	216
800°C, 30 min, air cool	215	211	204	210	213	221	222	219	216	222	185*	219
750°C, 15 min, air cool	699	715	699	704	254	261	260	258	249	247	247	248
750°C, 1 hour, air cool	236	232	226	231	229	230	231	230	230	224	216	223
750°C, 2 hour, air cool	227	226	296*	226	218	221	219	219	205	205	177*	205
700°C, 15 min, air cool	724	694	707	707	429	452	464	448	319	319	310	316
700°C, 1 hour, air cool	707	702	715	708	249	264	258	257	227	254	245	242
650°C, 15 min, air cool	777	724	649*	750	686	715	698	700	488	530	530	516
650°C, 1 hour, air cool	548	525	566	546	374	397	413	395	336	360	373	356
650°C, 2 hour, air cool	618	644	494*	631	362	320	351	344	286	297	304	296
550°C, 30 min, air cool	734	738	777	750	758	762	743	754	768	813	707*	790
550°C, 1 hour, air cool	715	792	762	756	792	824	762	793	792	818	724*	805
550°C, 2 hour, air cool	702	724	772	733	830	813	711*	821	707	724	669*	715
700°C, 15 min, air cool followed by 900°C, 15 min, air cool	207	215	222	215	238	238	236	237	274	264	272	270

*Outlying values--excluded from averages

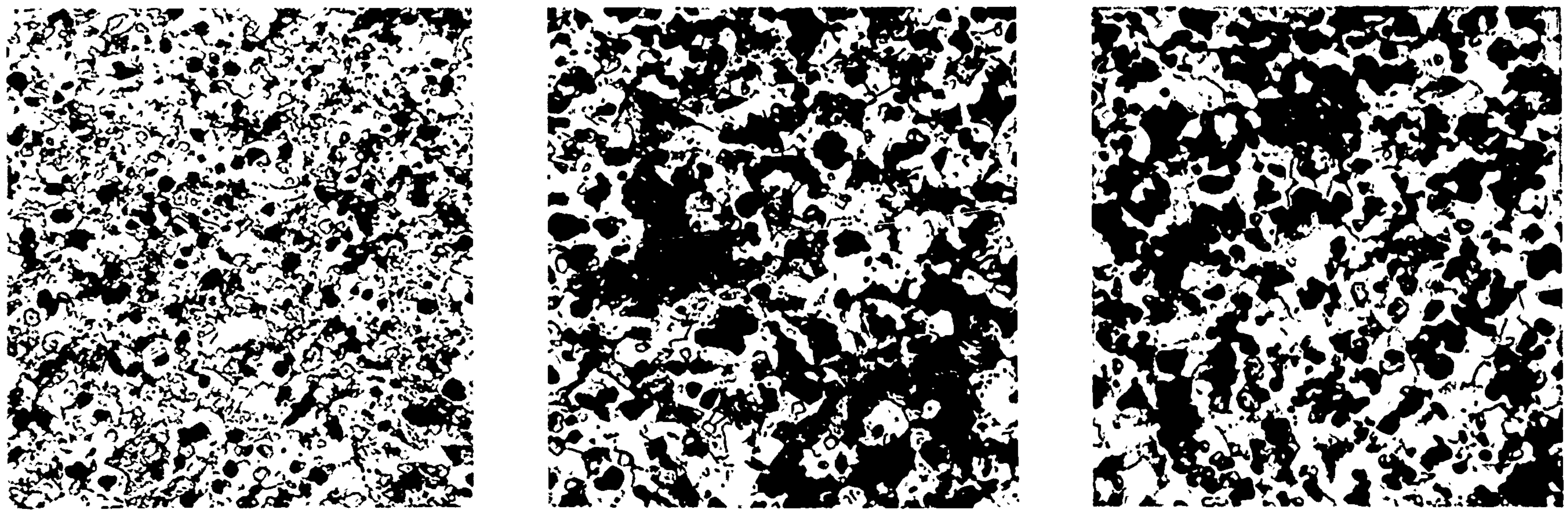


1/8" Section
Avg Hardness = 232 DPH

1/16" Section
Avg Hardness = 225 DPH

1/32" Section
Avg Hardness = 223 DPH

<u>Figure 97</u> - Microstructure of 4.25% CE iron, die cast and annealed 15 minutes at 900°C (1652°F) and air-cooled, 1000X magnification.

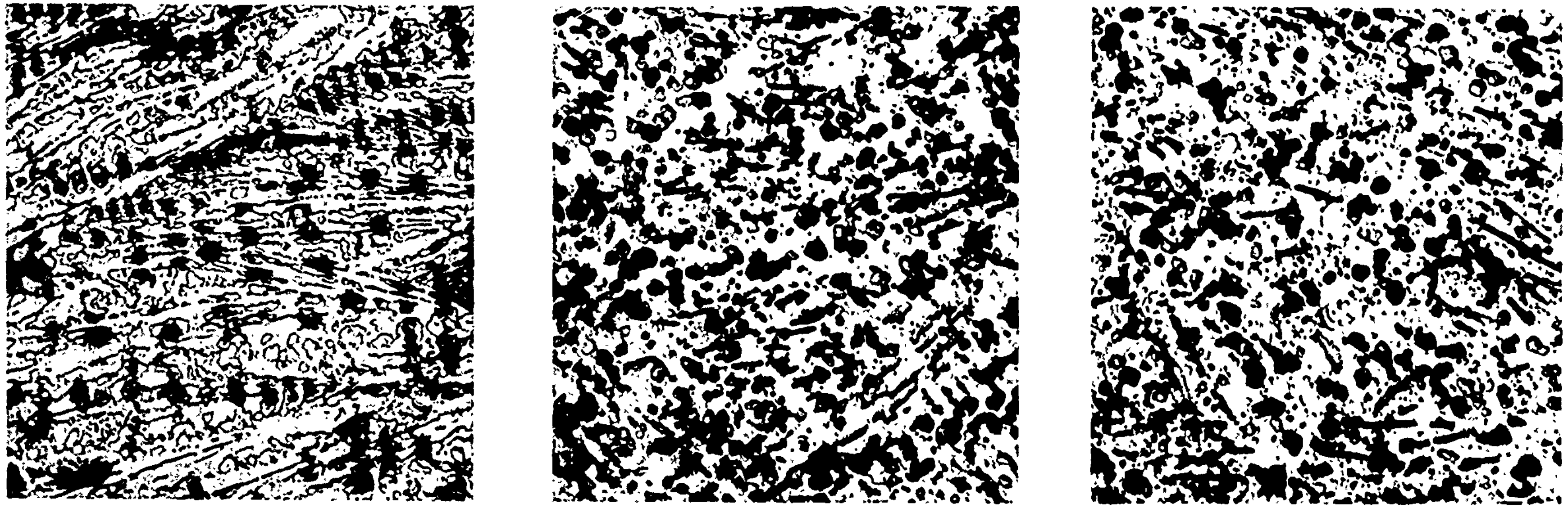


1/8" Section
Avg Hardness = 233 DPH

1/16" Section
Avg Hardness = 241 DPH

1/32" Section
Avg Hardness = 264 DPH

<u>Figure 98</u> - Microstructure of 4.25% CE iron, die cast and annealed two hours at 900°C (1652°F) and air-cooled, 1000X magnification.



1/8" Section
Avg Hardness = 708 DPH

1/16" Section
Avg Hardness = 257 DPH

1/32" Section
Avg Hardness = 242 DPH

Figure 99 - Microstructure of 4.25% CE iron, die cast and annealed one hour at 700°C (1292°F) and air-cooled, 1000X magnification.

Copper-free pig iron, containing 4.4% carbon, 0.10% manganese, 0.029% phosphorus, 0.028% sulfur, and 0.73% silicon, was used as the melt stock. In order to lower the carbon content, 15% Armco iron was added to the charge. To bring the silicon content to the desired level, an additional 1.4% was added as silicon metal.

A 100 pound charge was melted in a 200 pound capacity, MgO-lined induction furnace. The temperature of the melt was maintained at 2800°F, temperature measurements being taken with a platinum/platinum-rhodium immersion thermocouple in a fused silica protection tube. The carbon and silicon contents of the melt were determined immediately before inoculating the iron by the Leco and X-ray spectrographic techniques, respectively. The carbon content was found to be 3.6%; the silicon content was 2.1%.

Two methods of inoculation (i.e., adding magnesium to the melt) were investigated:

a. Ladle inoculation

b. Furnace inoculation

Ladle inoculation was affected by placing 10 to 20 grams of the iron-silicon-magnesium inoculant plus 5 to 7 grams of silicon in the bottom of a small, preheated, malleable iron hand ladle, coated with Arco-Perm 100. Approximately 2.2 pounds of molten iron were then poured into the ladle on top of the inoculant. The resultant, exothermic reaction was given a few seconds to subside, and then the inoculated metal was transferred directly to the shot sleeve of the die casting machine.

A nickel-magnesium inoculant was employed for the furnace-inoculation experiment. The inoculant was wrapped in aluminum foil and dropped into the bath. The bath was then given a post-inoculation with ferrosilicon, containing 36.6% silicon. Preheated, ceramic-lined, malleable iron hand ladles were employed to dip the molten iron from the furnace and transfer it to the shot sleeve of the die casting machine.

The amount of magnesium added by each technique was approximately 0.12%. A half dozen castings were made by each method of inoculation to insure reproducibility.

The castings, which had a wedge-shaped section, varied in thickness from 3/16" to 21/32". They were made in a high-density, pressed and sintered molybdenum die maintained at 700°F. An inspection of the fracture of the castings seemed to indicate that they had a white-iron structure throughout, although subsequent examination at 1000X revealed the existence of a few, very small graphite spherulites, even in the as-cast condition.

Two annealing treatments were assessed:

a. 1/2 hour at 1700°F, air cool

b. 3-1/2 hours at 1700°F, air cool.

The 1/2 hour anneal produced a pearlitic nodular iron , as indicated by Figure 100. A comparison of the unetched structure of annealed die cast nodular iron with the unetched, sand cast structure of a 1" nodular iron Y bar (illustrated in Figure 101) indicates, again, the advantage of die casting in producing refined microstructures.

The 3-1/2 hour anneal produced a ferritic nodular iron.

Tensile specimens resembling sheet specimens were cut from the area near the thinner edge of the wedge-shaped, nodular iron die castings (an area of assured soundness). Because of the size limitation imposed by the cast part, a specimen with a 1" length, not requiring pin holes, was adopted. No attempt was made to standardize the specimen thickness, but it was approximately 1/8". The tensile tests were made on an Instron machine using self-adjusting sheet grips. An 0.02 in/min/rate of cross-head travel was selected to yield a strain rate approximately equal to 0.02 in/in/min.

The results of the tensile tests are summarized in Table XXIII.

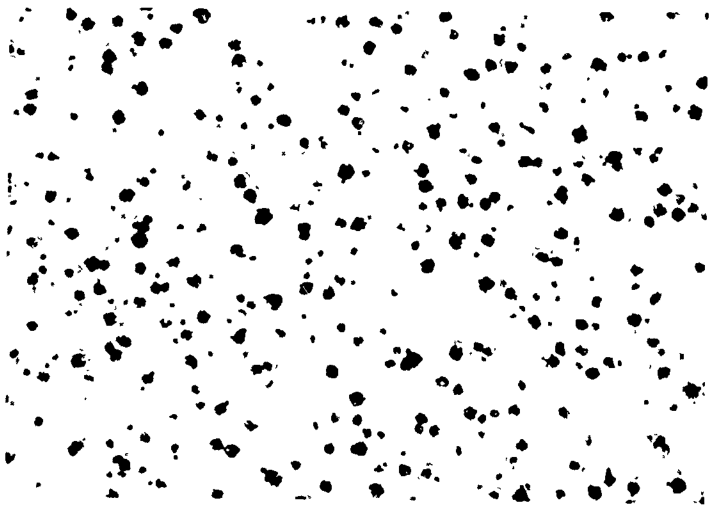
Although the feasibility of die casting iron of a nodular iron composition which will transform to nodular iron upon annealing was clearly established, the product has no clear-cut advantage over die cast malleable iron. The necessity of inoculation, however, is clearly a disadvantage.

The die casting operation depends upon maintaining a supply of molten metal from which small quantities can be periodically ladled or otherwise withdrawn. The loss of magnesium from molten nodular iron by oxidation and/or evaporation (fading) is rapid, completely negating the effect of the inoculant in 5 to 15 minutes. This makes furnace inoculation completely impractical for die casting. Ladle inoculation is more practical metallurgically, but from the economic point of view, it has several distinct disadvantages when compared to die cast malleable iron:

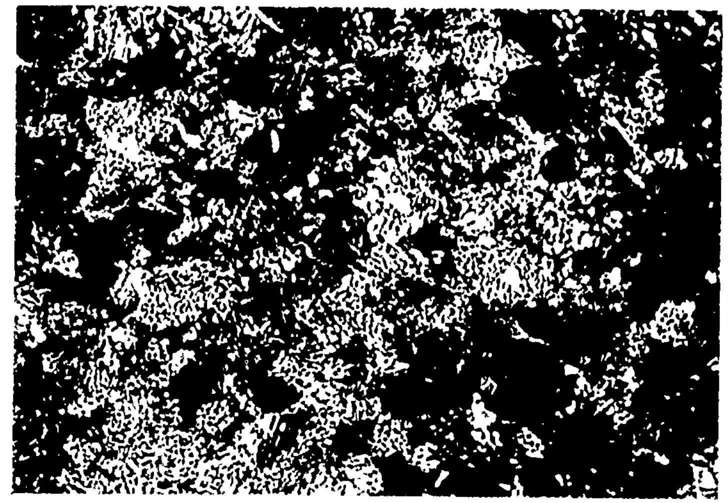
a. The inoculant adds an increment of cost to each casting

b. Inoculation introduces an extra operation to each casting cycle, increasing the cycle time and, thus, the cost per casting

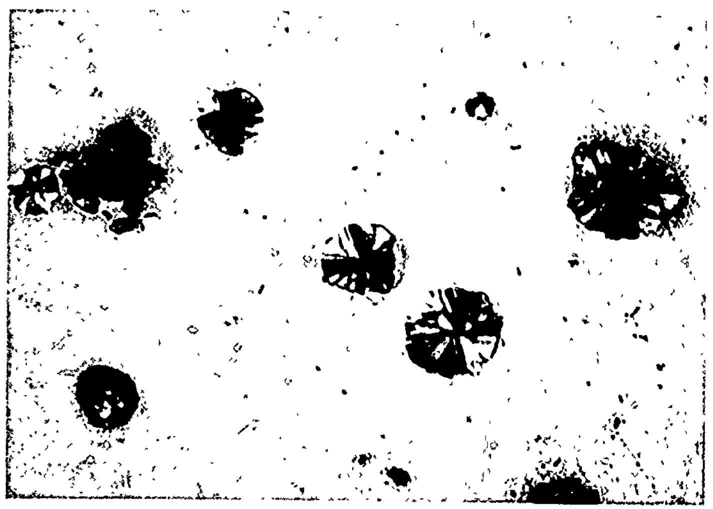
c. Nodular iron scrap from biscuits, runners, and overflows cannot be simply recycled, but must be mixed with steel scrap to lower the silicon analysis (or nickel--depending upon the inoculant used).



As Polished Mag. = 100X

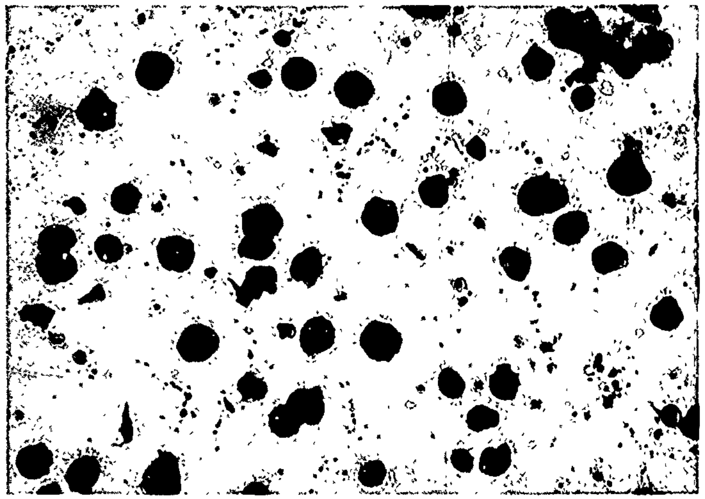


Etchant: Nital Mag. = 500X



As Polished Mag. = 500X

Figure 100 - Die cast nodular iron, annealed 30 minutes at 1700°F.



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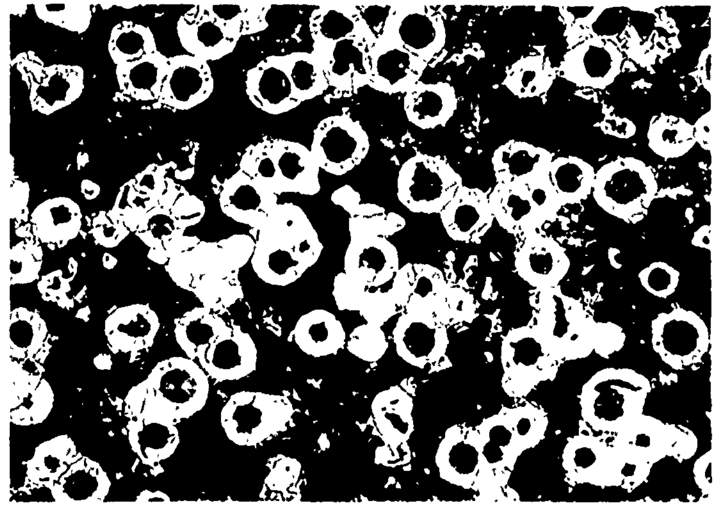

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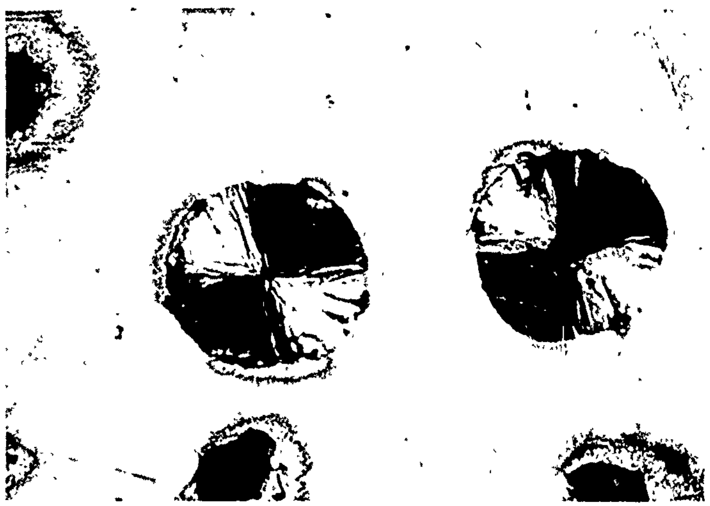

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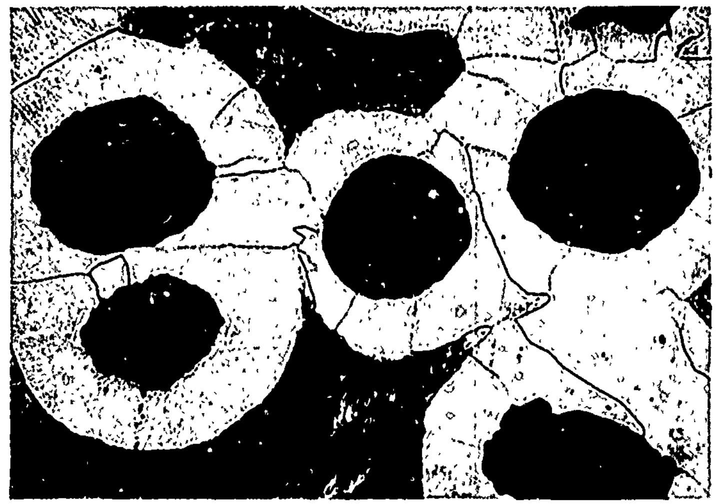


Etchant: Nital Mag. = 500X

Figure 101 - Nodular iron as sand cast into a 1" Y-bar mold.

Table XXIII--Mechanical Properties of Die Cast Nodular Iron

Annealing Treatment	Hardness (BHN)	UTS (psi)	0.2% YS (psi)	Elongation (% in 1")
1/2 hour at 1700°F	260	103,000	76,500	5
3-1/2 hours at 1700°F	150	57,700	36,400	8.2

One might speculate that, with a sufficiently high die temperature, a significant fraction of the total carbon might form graphite sperulites on solidification. The effect of such a high temperature on die life is speculative, too, but it would probably be adverse. What is clear is that such an approach would require an extended dwell time, measurably increasing overall cycle time.

3. MALLEABLE IRON

A carbon equivalent of 3.8% would conventionally classify a cast iron as grey iron. Grey iron, in the usual sense, however, cannot be produced by die casting. As indicated previously, even eutectic iron solidifies without the formation of free carbon when it is die cast. For convenience, therefore, the 3.8% CE iron produced under this contract will be referred to as malleable iron. Compare Figure 102, which illustrates the structure of the 3.78% CE iron die cast by Dort, with the structure of a 0.505" diameter tensile bar sand cast from the same melt, illustrated by Figure 103. The die cast iron is white; the sand cast iron is grey.

None of the samples die cast from 3.78% CE iron by Dort in cooperation with the Research Center was suitable for tensile testing. Therefore, no tensile data were generated for the alloy.

Doehler Jarvis, Dort, and the Research Center all cast 3.1% CE malleable iron based on the Moline Malleable Iron Company's standard composition, i.e., 2.5% carbon, 1.5% silicon, 0.5% manganese, balance iron. Figure 104 reveals the structure that resulted from die casting 3.1% CE iron in the interim test bar die at Dort.

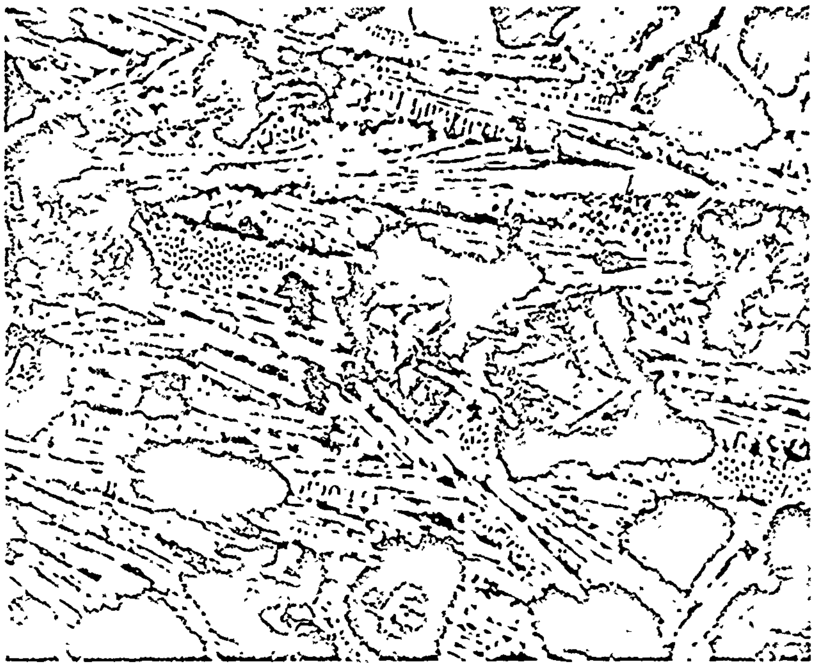
Typically, a malleable iron supplier, such as the Moline Malleable Iron Company, produces both ferritic and pearlitic malleable iron from material of the same composition. Indeed, both grades may first be given the same ferritizing anneal. Those castings which must have a pearlitic structure can then be transformed from ferritic to pearlitic malleable iron by a subsequent annealing treatment, or pearlitic malleable iron can be formed in a single-stage anneal.

Batch annealing is still very common practice among malleable iron suppliers as a method for producing ferritic malleable iron. The conditions under which Moline performs their batch annealing are approximately:

a. Heat slowly to 1620°F (882°C)

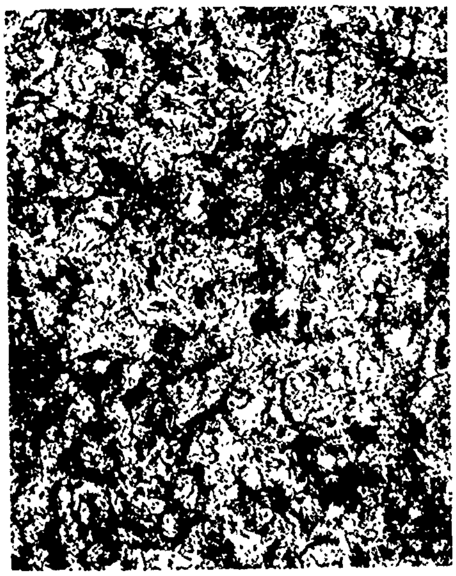
b. Hold at 1620°F for 40 hours

c. Cool rapidly to 1450°F (788°C)



Mag. = 1000X

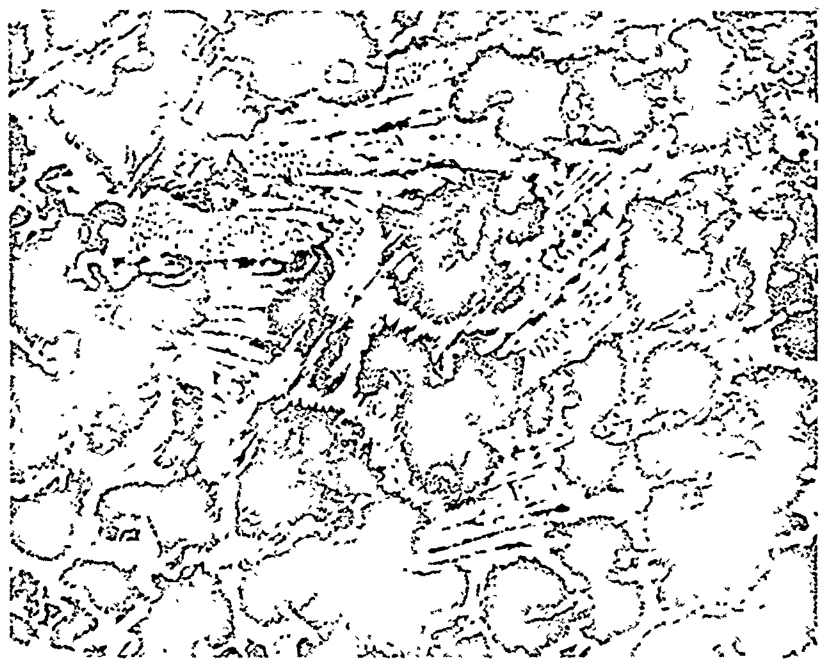
Figure 102 - Microstructure of 3.78% CE iron, as die cast by Dort in cooperation with the Research Center. Average hardness = 654 DPH, 48.8 R_C



Mag. = 100X

Mag. = 500X

Figure 103 - Microstructure of a 0.505" diameter tensile bar sand cast from 3.78% CE iron.



Mag. = 1000X

Figure 104 - Microstructure of 3.1% CE malleable iron, as die cast by Dort in cooperation with the Research Center. Average hardness = 600 DPH, 47.6 R_C

d. Cool at 10° per hour to 1250°F (677°C)

e. Hold for six hours at 1180°F (638°C)

f. Air cool to room temperature.

To convert ferritic malleable iron to pearlitic malleable iron, a supplier might austenitize the castings at 1550°F to 1600°F (843°C to 871°C) and cool in an air blast or quench in hot oil. Figures 105 and 106 represent typical microstructures for sand cast malleable iron, and Table XXIV indicates the room temperature mechanical properties that can be expected from sand cast 3.1% CE malleable iron in the ferritic and the pearlitic grades.

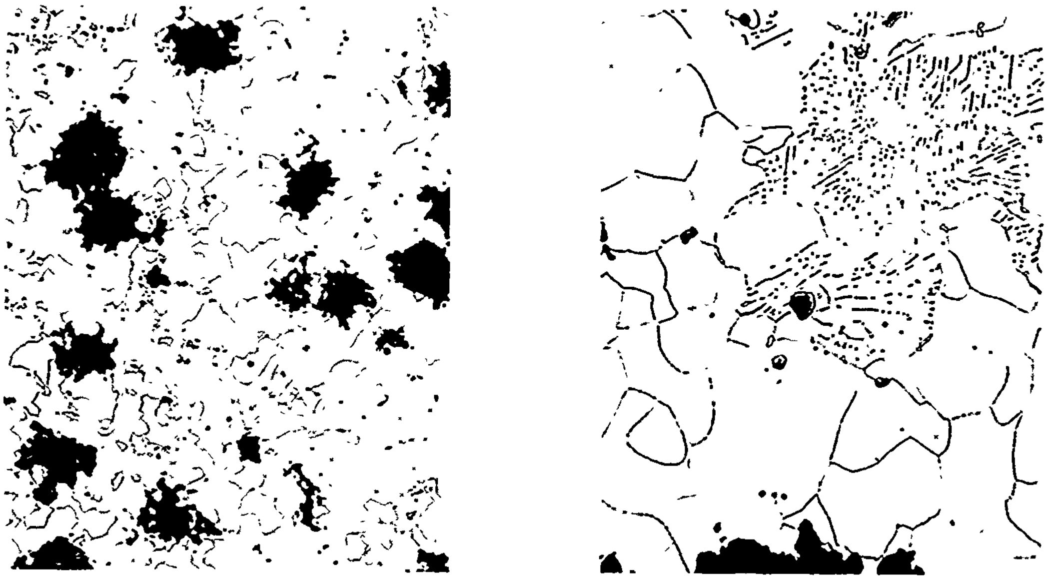
The attempt by the Research and Development Center to apply the information obtained in the study of the heat treatment of 4.25% CE iron to the heat treatment of 3.1% CE malleable iron was largely unsuccessful. Complete graphitization of the 3.1% CE malleable iron does not occur in times of 30 to 60 minutes as it does in the 4.25% CE iron. Instead, it was found that times as long as four hours at 900°C (1652°F) may be the minimum required to completely graphitize 3.1% CE iron and eliminate all carbides. Figures 107 and 108 illustrate the microstructures developed by subjecting the die cast 3.1% CE malleable iron to pearlitic and ferritic malleablizing heat treatments, respectively. Figure 109 is a photomacrograph of a section through the 1/2" diameter shoulder of a 3.1% CE malleable iron tensile bar die cast by Doehler. It was annealed four hours at 900°C (1652°F), furnace cooled to 700°C (1292°F), held for eight hours, and air cooled. At higher magnification, the obvious central core was found to contain large quantities of undissolved primary carbides, indicating that longer times and/or higher temperatures are required to malleablize 1/2" sections of die cast 3.1% CE iron.

Table XXV indicates the mechanical properties that can be expected from die cast 3.1% CE malleable iron.

In cooperation with the Research Center, 2.6% CE malleable iron was also die cast by Dort. The as-die-cast structure of that material may be seen in Figure 110. No mechanical testing was performed on castings of this composition.

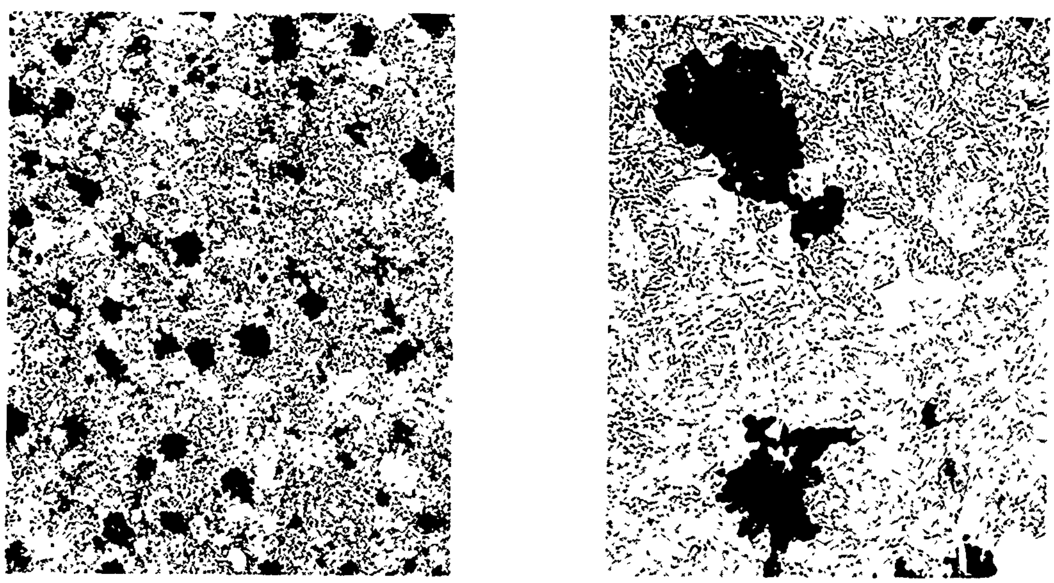
4. AISI 4340 STEEL

A number of die castings of what was nominally 4340 were made by Dort, in cooperation with the Research Center. As noted in Section V and documented by Table XIX, the carbon, manganese, and silicon contents



Mag. = 100X Mag. = 500X

Figure 105 - Microstructure of commercial, sand cast 3.1% CE ferritic malleable iron.



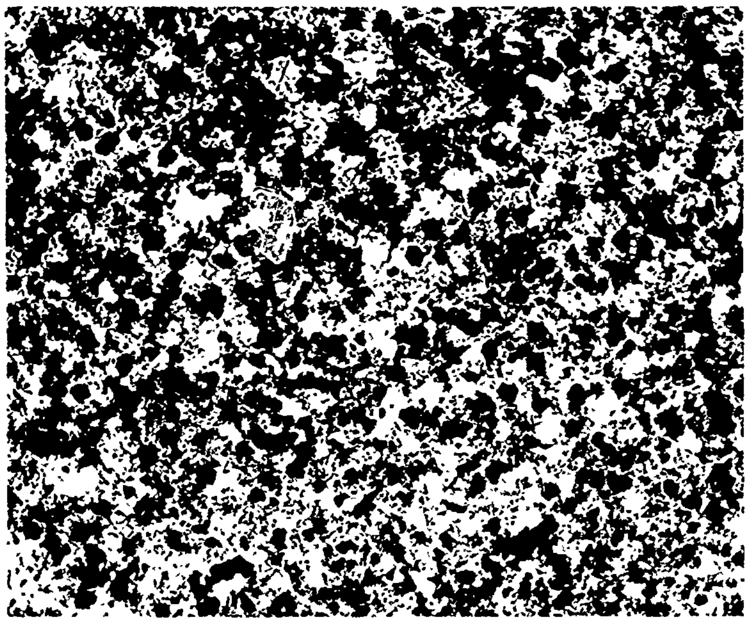
Mag. = 100X Mag. = 500X

Figure 106 - Microstructure of commercial, sand cast 3.1% CE pearlitic malleable iron.

Table XXIV--Mechanical Properties of Commercial, Sand Cast 3.1% CE Malleable Iron

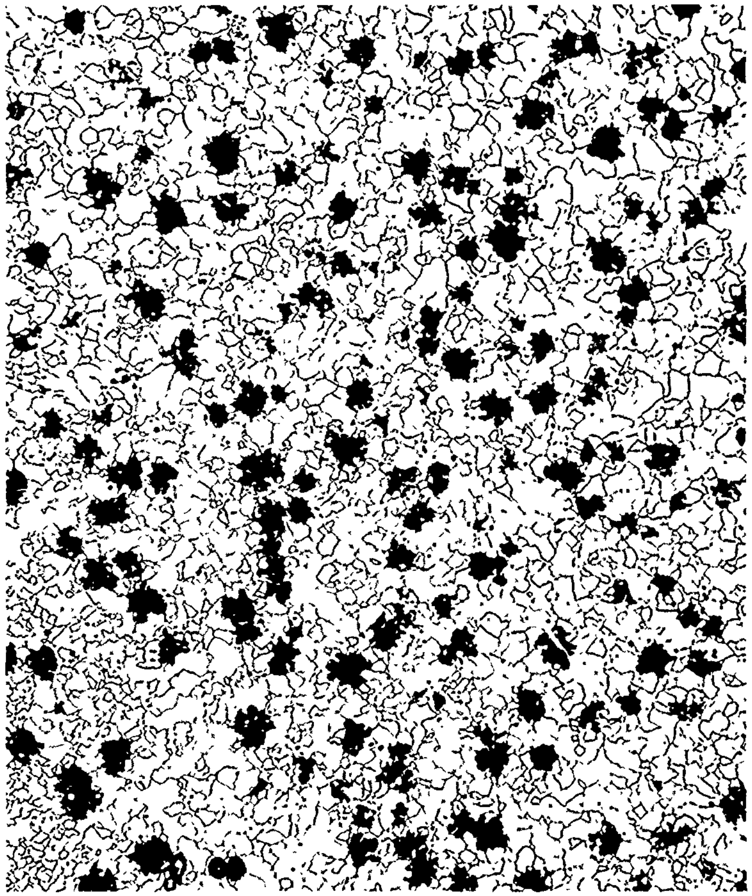
Grade	Hard-ness (R_B)	UTS (ksi)	0.2% YS (ksi)	Elon-gation (%)	Red of Area (%)	Impact Strength* (ft - lbs)
Ferritic	94	51	25	15	17	10-11
Pearlitic	67	101	91	5	5	4.2-4.5

*Charpy, V-notch



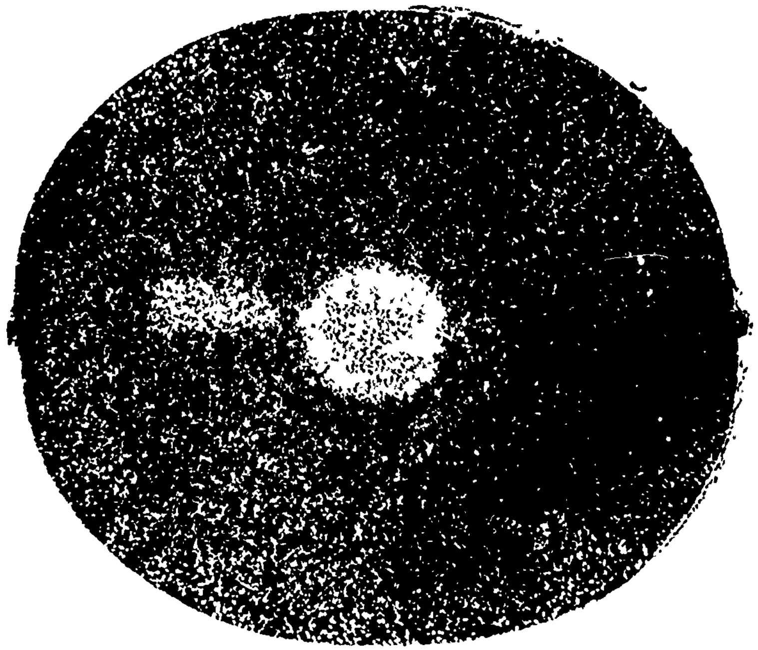
Etchant: Nital Mag. = 100X

Figure 107 - Microstructure of 3.1% CE malleable iron, die cast by the Research Center, annealed four hours at 1650°F (900°C), and air-cooled to develop a pearlitic structure.



Etchant: Nital Mag. = 100X

Figure 108 - Microstructure of 3.1% malleable iron, die cast by the Research Center, annealed four hours at 1650°F (900°C), furnace cooled to 1290°F (700°C), held for eight hours, and air-cooled to develop a ferritic structure.



Etchant: Nital Mag. = 7.5X

Figure 109 - Photomacrograph of the section through the 1/2" diameter shoulder of a 3.1% CE iron test bar, annealed four hours at 1650°F (900°C), furnac cooled to 1290°F (700°C), held for eight hours, and air-cooled to devel a ferritic structure.

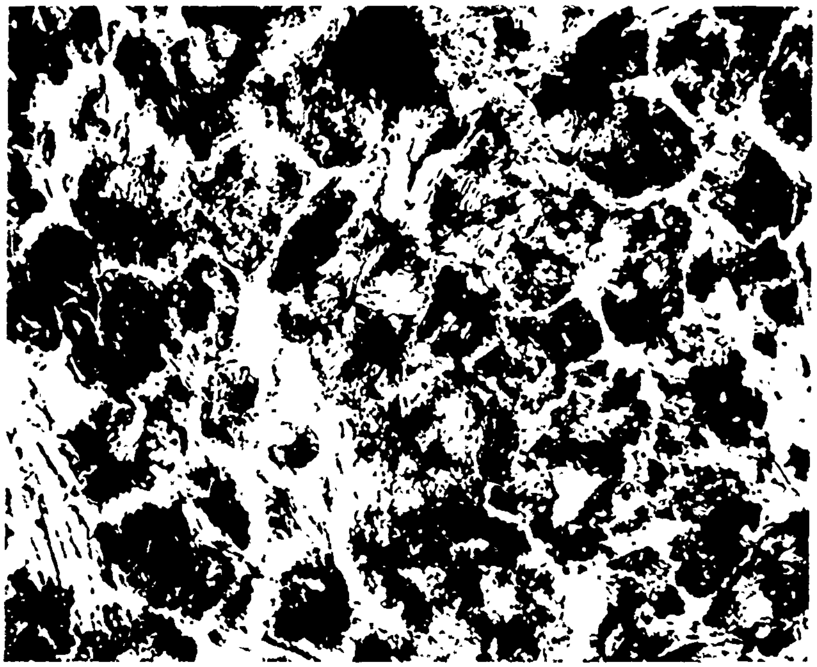
Table XXV--Room Temperature Mechanical Properties of Die Cast 3.1% CE Malleable Iron

Heat Treatment	Type	Source	Rockwell Hardness	Impact Strength (ft-lbs)	UTS (ksi)	0.2% YS (ksi)	% Elong*
Heat to 1750°F in 1 hour,	Pearlitic	GERDC	--	5.0	92.5	56.5	4.1
hold for 5 hours, cool to	Pearlitic	GERDC	--	5.3	100.0	62.5	3.1
1450°F in 1 hour, air	Pearlitic	GERDC	--	4.7	94.5	59.0	4.8
cool to room temperature	Pearlitic	Doehler	--	--	103.5	53.5	6.2
Heat to 1650°F in an air	Pearlitic	GERDC	32_C	2.5	98	73	1.5
furnace, hold for 4 hours	Pearlitic	GERDC	31_C	2.5	103	79	1.5
and air cool	Pearlitic	Doehler(a)	28_C	--	89.5	73.0	1.5
	Pearlitic	Doehler(b)	27_C	--	132.5	98.7	2.5
Heat to 1750°F in 1 hour,	Ferritic	GERDC	--	6.0	77	60	8
hold for 5 hours, cool to	Ferritic	GERDC	--	7.1	50	42	7
1450°F in 1 hour, hold for	Ferritic	GERDC	--	6.9	74	47	6
1 hour, cool at 10°F per	Ferritic	Doehler	--	--	83	50	4
hour to 1340°F, air cool							
to room temperature							
Heat to 1650°F under	Ferritic	GERDC	69_B	8	40.5	32.5	**
argon, hold for 4 hours,	Ferritic	GERDC	74_B	8.5	54	30	7
furnace cool to 1290°F,	Ferritic	GERDC	71_B	9.5	55.5	32	7
hold for 8 hours, furnace	Ferritic	Doehler(b)	55_B	--	47.0	28.6	13
cool to room temperature	Ferritic	Doehler(c)	53_B	--	38.2	27.2	6

*The samples produced by the Research Center (GERDC) had a 1" gage length; those produced by Doehler had a 2" gage length.

**This specimen broke in the radiused section through a large hole that extended into the radiused section from the grip section.

(a) This sample exhibited a severe defect in the fracture.
(b) These samples exhibited sound fracture surfaces.
(c) This sample exhibited a cluster of defects in the fracture.



Mag. = 1000X

Figure 110 - Microstructure of 2.6% CE malleable iron, as die cast by Dort in cooperation with the Research Center.
Average hardness = 522 DPH, 40.7 R_C

of the castings were far lower than the standard for AISI 4340. The mechanical test results for the material are presented in Table XXVI, and the as-cast microstructures are illustrated by Figure 111. Neither should be considered to be representative of die cast AISI 4340. They are presented here only as a matter of record.

5. STAINLESS STEEL

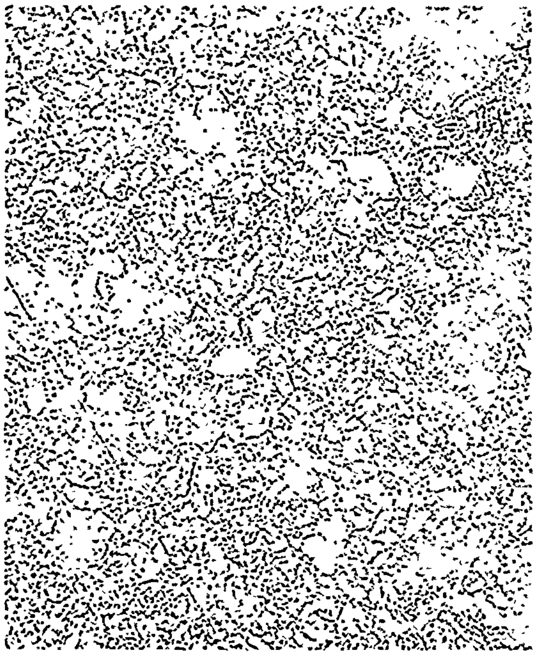
Both AISI 304 and AISI 403 stainless steel were cast by Doehler Jarvis and the Dort Metallurgical Company, in cooperation with the Research Center. Figures 112 and 113 illustrate the as-cast microstructures of the stainless die cast by Dort.

Table XXVII presents the results of mechanically testing two AISI 304 stainless steel die castings; one produced by Doehler Jarvis, and one by Dort, in cooperation with the Research and Development Center. The properties of the two samples were in good agreement, but the sample produced by Doehler Jarvis exhibited a surprising variation in hardness as a function of section size. It is tempting to hypothesize that the higher hardness values observed in the small sections are related to the unusual, as-die-cast structures seen in Figure 112. One might further hypothesize that, in heavy sections, that structure is effectively self-annealed. No additional effort was made, however, to confirm or refute the hypothesis.

From both the appearance of the microstructure in Figure 113 and the hardness values reported in Table XXVIII, it was concluded that the die casting process produces a martenstic structure in AISI 403 stainless steel, which accounts for the lack of ductility in the as-die-cast condition. It must be borne in mind, when considering the properties reported in Table XXVIII, however, that the material produced by Dort was not truly AISI 403 stainless but an alloy having a higher carbon content and a lower chromium content than standard AISI 403 stainless. The other noteworthy aspects of these data are that the cast-to-shape test bar, annealed at 1400°F for two hours, was appreciably stronger than the specimen machined from a runner after the same heat treatment. No difference in ductility was noted. The difference in strength was attributed to the difference in cast section size and the effect that section size has on solidification and cooling rates, and thus on grain size.

Table XXVI--Room Temperature Mechanical Properties of Die Castings Produced from an Off-Chemistry Melt of AISI 4340

Heat Treatment	Rockwell Hardness	Impact Strength (in-lbs)	UTS (ksi)	0.2% YS (ksi)	% R.A.
As-die cast	95_B	5.5	--	--	--
Austenitized at 1525°F, oil quenched	39_C	--	--	--	--
Hardened as above, tempered at 600°F	56_B	--	74	49	39
Hardened as above, tempered at 800°F	50_B	32	--	--	--
Hardened as above, tempered at 1000°F	91_B	25	82	66	43
Hardened as above, tempered at 1200°F	60_B	39	--	--	--

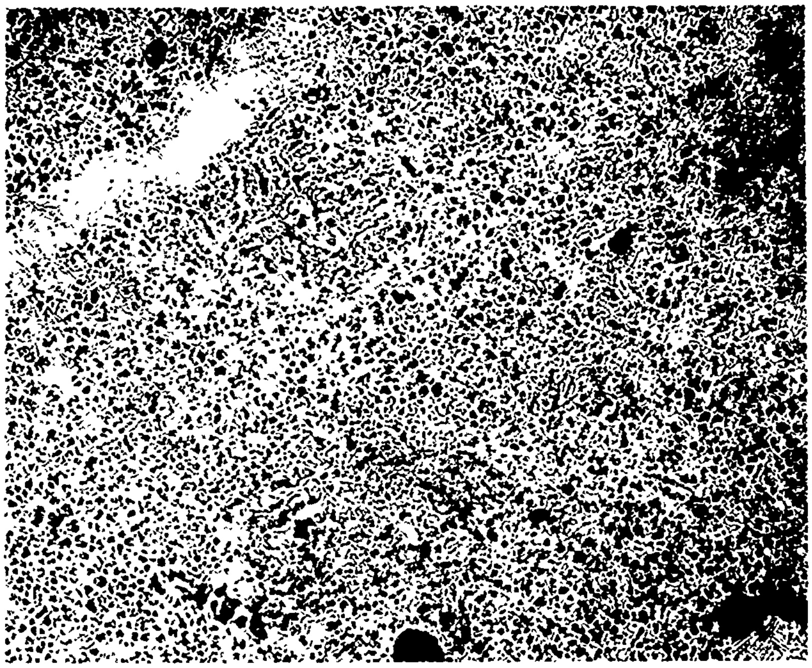


Mag. = 100X

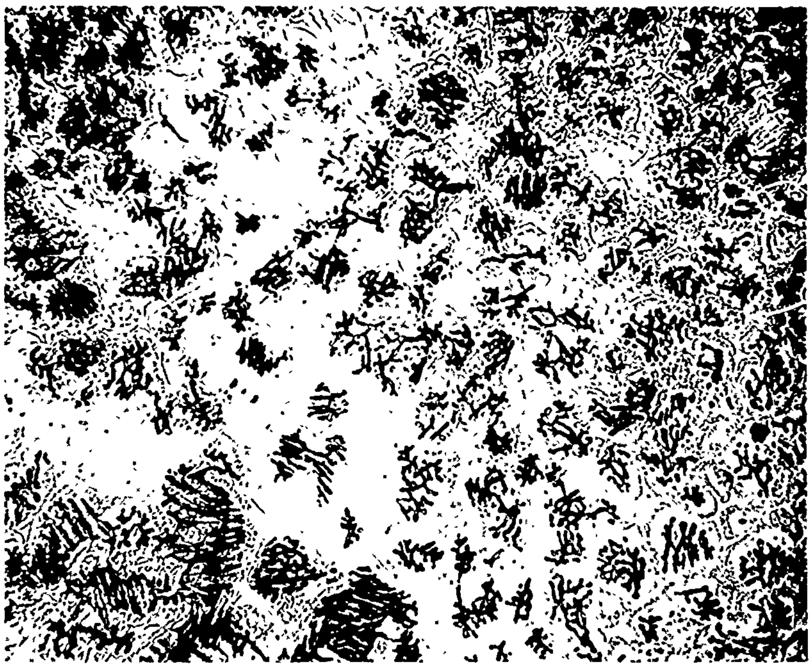
Mag. = 500X

Mag. = 1000X

Figure 111 - Microstructure of off-chemistry 4340 steel, as die cast by Dort in cooperation with the Research Center. Average hardness = 243 DPH, 95.0 R_B

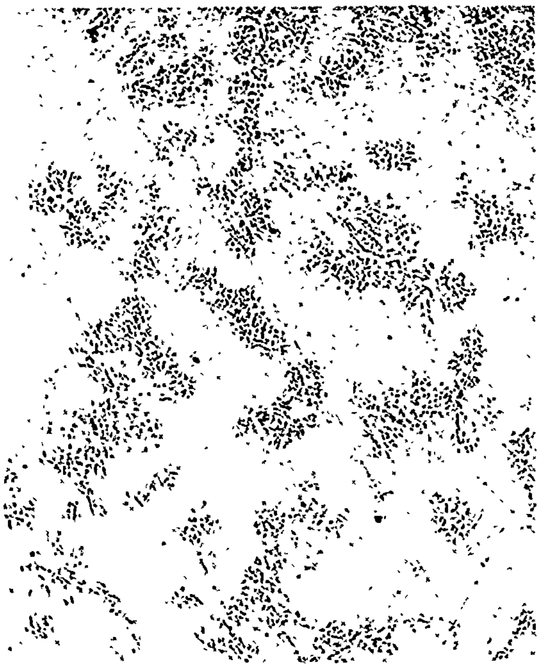


Mag. = 100X

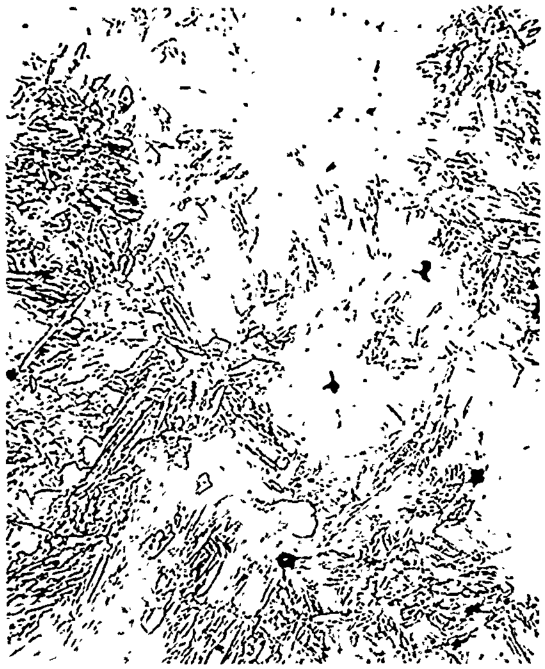


Mag. = 500X

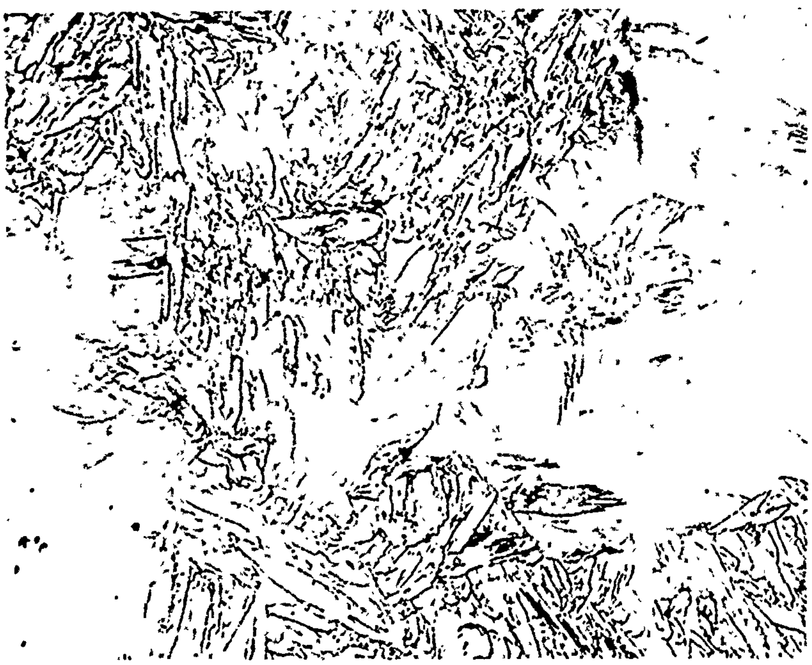
Figure 112 - Microstructure of 304 stainless steel, as die cast by Dort in cooperation with the Research Center. Average hardness = 206 DPH, 78.6 R_B



Mag. = 100X



Mag. = 500X



Mag. = 1000X

Figure 113 - Microstructure of 403 stainless steel, as die cast by Dort in cooperation with the Research Center.
Average hardness = 645 DPH, 48.1 R_C

Table XXVII--Room Temperature Mechanical Properties of AISI 304 Stainless Steel As-Die Cast

Source	Hardness (R_B)	UTS (ksi)	0.2% YS (ksi)	Elongation (%)	Red of Area (%)
Doehler	1/4" dia 84-87.5 7/16" dia 83 9/16" dia 79-80	88.9	(a)	40(b)	45.6
Dort/GERDC	74.5-82.8	86	44	47(c)	51

(a) Not determined

(b) In a 2" gage length

(c) In a 1" gage length

Table XXVIII--Room Temperature Mechanical Properties of Die Cast AISI 403 Stainless Steel

Heat Treatment	Source	Location	Hardness	UTS (ksi)	0.2% YS (ksi)	Elongation (% in 1")	Impact Strength (ft-lbs)
As die cast	Dort/GERDC	Test bar	48 R_C	(a)	(a)	(a)	(a)
As die cast	Doehler	Test bar	1/4" dia 45-46 R_C 7/16" dia 49-51 R_C 9/16" dia 49-51 R_C	84.8	(b)	nil	(a)
2 hrs at 1110°F, air cool	Dort/GERDC	Test bar	96 R_B	(a)	(a)	(a)	5
2 hrs at 1110°F, air cool	Dort/GERDC	Runner	(a)	99	98	2	(a)
2 hrs at 1220°F, air cool	Dort/GERDC	Test bar	86 R_B	(a)	(a)	(a)	14
2 hrs at 1220°F, air cool	Dort/GERDC	Runner	(a)	97.5	84.5	4	(a)
2 hrs at 1400°F, air cool	Dort/GERDC	Test bar	85 R_B	95	69	30(c)	27
2 hrs at 1400°F, air cool	Dort/GERDC	Runner	(a)	76	62	7	(a)

(a) Not determined

(b) Did not yield--brittle fracture

(c) Percent reduction in area

SECTION VII

DIE MATERIALS EVALUATION

Die casting exists as a commercial metalworking process, not because die castings possess any technical advantage over parts made by other processes, but because there are many parts that can be produced more inexpensively by die casting than by any other process. Ferrous die casting has only recently become a practical prospect, because until very recently, the economics of the process were forbidding.

Die making is a precise and painstaking craft, and even simple die casting dies are costly. That cost must be amortized over the useful life of the dies. If sufficiently long die lives are achieved, the economics of the die casting process become very attractive; if sufficiently long die lives are not achieved, the economics become very unattractive. Conventional die materials, such as hot-work die steels, had been found to be completely unsatisfactory as dies for ferrous die casting. Only when consideration was given the prospect of constructing ferrous die casting dies from refractory metals did the prospects improve noticeably.

For ferrous die casting, die life is the sine qua non. This contract, therefore, explored the subject of die life and die materials in great detail.

The theoretical basis for the selection of a die material has been presented in detail by Noesen and Williams.[17] Each time liquid metal is injected into a die, the surface of that die undergoes a thermal excursion, the magnitude of which depends on the temperature of the die, the temperature of the metal, and the heat diffusivity of each. (Heat diffusivity is the square root of the product of the thermal conductivity multiplied by the density and the specific heat capacity.) The thermal excursion produces a strain in the surface of the die which is dependent on the coefficient of thermal expansion of the die. That strain has an elastic component, which may be disregarded, and a plastic component which, when reapplied, cycle after cycle, leads to failure by fatigue. The magnitude of the plastic strain depends on the magnitude of the thermal excursion and on the strength, ductility, and clastic modulus of the die.

Having determined the failure mechanism, it is possible to describe a model die material. It should have the following characteristics:

a. High thermal conductivity

b. High density

c. High specific heat capacity

d. Low thermal expansion

e. High strength

f. High ductility

g. Low elastic modulus.

No one material fits this model perfectly. Tool steels, like H-13, have been pushed to their limit by brass die casting. Their chief virtue has been their strength. Copper, which has been used for ingot stools and continuous casting dies in the steel industry, has a limited useful lifetime before failing by thermal fatigue. The virtues of copper are its high thermal conductivity, high ductility, and low elastic modulus. The refractory metals, columbium, tantalum, molybdenum, and tungsten stood out as potentially interesting but untried die materials. All have low coefficients of thermal expansion, and tungsten and molybdenum also exhibit notably high thermal conductivities. The initial goal of this contract, therefore, was to evaluate six commercially available molybdenum and tungsten base die materials. The six selected were:

Molybdenum Base Die Materials

a. High-density, pressed and sintered molybdenum

b. Wrought, powder metallurgy molybdenum

c. Wrought, arc-cast TZM

Tungsten Base Die Materials

a. Pressed and sintered tungsten (and/or tungsten-2% ThO_2)

b. Copper-infiltrated tungsten

c. Anviloy 1150.*

When the Lamp Metals and Components Department relieved Doehler Jarvis of responsibility for evaluating die materials, they had a more comprehensive, in-house materials evaluation underway. By combining the two evaluations, it was possible to greatly enlarge the scope of the program. In the course of that program, 16 different materials were evaluated.

*Anviloy 1150 is a product of the Mallory Metallurgical Company, Indianapolis, Indiana.

a. High-density, pressed and sintered molybdenum--This is a pure molybdenum product which is given a special sintering treatment to assure a minimum density of 97% of theoretical. It is commercially available.

b. Wrought molybdenum--This is a pure molybdenum, powder metallurgy product which is subjected to plastic deformation at elevated temperatures below its recrystallization temperature. This results in a fully dense, "cold worked" structure which is stronger than the powder metallurgy product. Although it is somewhat less ductile than the powder metallurgy product, above the ductile-to-brittle transition temperature of the latter, the "cold work" lowers the ductile-to-brittle transition temperature of the wrought product, making it more ductile than the powder metallurgy product in the temperature range between the ductile-to-brittle temperatures of the two materials. Wrought molybdenum is typically an anisotropic product. It is commercially available.

c. Diffusion silicided, wrought molybdenum--Ductile $MoSi_2$ was formed on the surfaces of this insert by the electrodeposition of silicon from a fused fluoride bath accompanied by the interdiffusion of silicon and molybdenum.

d. TZM--This is a vacuum arc melted, solid solution and dispersion strengthened, wrought molybdenum alloy of the following nominal composition: 0.5% titanium, 0.1% zirconium, balance molybdenum. Like wrought molybdenum, it is typically anisotropic and commercially available.

e. Mo-3--This is an experimental, wrought molybdenum alloy.

f. Copper-infiltrated molybdenum--This experimental material was produced by vacuum infiltrating a sintered molybdenum skeleton with copper-2% beryllium.

g. HOT SHOT 2920X--This is an experimental, pressed and sintered molybdenum-base alloy.*

h. 80-20--This experimental, pressed and sintered binary alloy is composed of 80% molybdenum and 20% tungsten.

i. Pressed and sintered tungsten--A commercially available, pure unalloyed grade of tungsten, known as machinable tungsten.

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*HOT SHOT is a trademark of the General Electric Company for die materials.

j. Tungsten-2% thoria--This is a commercially available, pressed and sintered product containing a dispersion of two weight percent thoria which increases the strength, ductile-to-brittle transition temperature, and machinability of the product.

k. Anviloy 1150--This is a commercially available liquid-phase-sintered alloy consisting of a high-tungsten aggregate bonded by a molybdenum, nickel, iron, tungsten alloy network. The nominal, overall composition of the product is 4% molybdenum, 4% nickel, 2% iron, balance tungsten.

l. Copper-infiltrated tungsten--Analogous to copper-infiltrated molybdenum, this commercially available product is produced by infiltrating a porous sintered tungsten skeleton with pure copper, yielding a product with higher thermal conductivity and vastly improved machinability.

m. Cb-25% zirconium--This material was electron beam melted and extruded. It is commercially available as wire.

n. Cb-752--This material, having a nominal composition of 10% tungsten, 2.5% zirconium, balance columbium, was electron beam melted and wrought. It is commercially available.

o. GE-474--This is an experimental, wrought tantalum-base alloy.

p. H-13--This hot work die steel, which is the standard of the aluminum die casting industry, has a nominal composition of 5.00% chromium, 1.20% molybdenum, 1.00% vanadium, 1.00% silicon, 0.40% carbon, 0.30% manganese, balance iron.

The design of the die in which the Lamp Metals and Components Department tested these materials was detailed in Section IV.

Machining operations related to die construction are known to introduce surface stresses in die components, the nature and magnitude of which depend upon the type of machining operation, the feed rate, the sharpness of the tool, etc. In dies, the interaction of machining stresses with the stresses introduced by the thermal cycle may be either beneficial or detrimental. For the die materials evaluation, the conservative position was taken that all components should be stress relief annealed before being put into service. Exceptions were made only to attempt to ascertain the importance of that procedure.

Before discussing the results of the die materials evaluation in detail, the various types of failure mechanisms or deterioration that were observed in the materials evaluation will be outlined.

a. Heat checking--This is the familiar "craze cracking," a thermal fatigue failure.

b. Gross cracking--This mechanism is manifested by isolated cracks seemingly unrelated to thermal fatigue. Its origin is not completely understood.

c. Brittle failure--There are three reasons for this type of failure:

    1. Careless handling of materials below their ductile-to-brittle transition temperature

    2. Random failure of die components operated below their ductile-to-brittle transition temperature

    3. Delamination of wrought refractory metals parallel to their direction of deformation.

d. Plastic deformation--Four different manifestations of this phenomenon were observed:

    1. Gaps are opened progressively at the interfaces between die components which are exposed to high-pressure, liquid metal

    2. Originally flat surfaces of die components not restrained by the lockup force of the die, but exposed to the liquid metal, tend to form raised replicas of the features of the opposing die, resembling welts

    3. The dies may be dented by closing up on flash or through the careless use of tools

    4. The cavities may be deformed by shrinking castings.

e. Soldering--This is the welding of the cast metal to the die material.

f. Erosion--This is the removal of material by the flowing molten metal.

g. Pitting--This appears to be the selective removal of some microconstituents of the die.

The Lamp Metals and Components Departments' materials evaluation die was first put into service with the materials indicated in Table XXIX. Two shots were made with 2.9% CE malleable iron. Spectacular flashing was encountered on each shot. The flash welded to the parting line of the insert retainer plates in the upper corner, away from the operator (i.e., the upper left corner of the cover half and the upper right corner of the ejector half in the face view) and had to be ground off. To rectify that situation, the ejector die was completely disassembled.

Because one of the ejector pins in the copper-infiltrated tungsten insert had seized, it was removed from the insert retainer plate. At that point, it was discovered that the shoulders formed between the retaining keyway and the bottom of the insert had broken away with a conchoidal fracture, making it impossible to use the insert without extensive modification.

Although the possibility exists that the insert failed due to careless handling, the grossness of the failure suggested that the properties of the material should be subjected to a thorough, critical review. When the tensile properties of the material were collected, they revealed the material to be extremely brittle. If the material has a ductile-to-brittle transition temperature, which is questionable, it is above 1500°F, as indicated by the data compiled in Table XXX.

Because brittle failure had been observed in die materials like tungsten-2% thoria, which were operated at temperatures below their ductile-to-brittle transitions (e.g., in Doehler Jarvis' hemisphere die), and because soldering becomes a major problem above 700°F, it was concluded that copper-infiltrated tungsten was not a promising die material.

On the basis of the limited information available, however, molybdenum infiltrated with 2% beryllium-copper appeared to be an interesting prospective die material. An indication of the mechanical properties of that material is given by the data in Table XXXI.

While awaiting the preparation of a copper-infiltrated molybdenum insert, however, a replacement-style, high-density, pressed and sintered molybdenum insert was placed in ejector Impression 4 as a replacement for the copper-infiltrated tungsten insert. The new insert was in the as-machined condition (i.e., not stress relief annealed after machining).

To avoid a repetition of the difficulties created by the flashing of the malleable iron, the die materials evaluation was resumed, using 304 stainless steel as the cast metal. The lower fluidity of the stainless steel, it was hoped, would make it less likely to flash than malleable iron; and earlier experience had already indicated that the 304 stainless was probably less likely to weld to refractory metal dies than was malleable iron.

Table XXIX --Materials in the LMCD Materials Evaluation Die at Shot Number 1

Position	Material	Condition	Insert Style	Remarks
Cover half	High-density pressed & sintered molybdenum	Stress relieved	Retainer plate	Plate has flat surface, no runners, overflows or vents
1	Anviloy 1150	Stress relieved	Original	--
2	High-density pressed & sintered molybdenum	Stress relieved	Original	--
3	Mo-3	Stress relieved	Replacement	--
4	80-20	Stress relieved	Replacement	--
5	TZM	Stress relieved	Replacement	--
6	High-density pressed & sintered molybdenum	As machined	Replacement	--
Ejector half	High-density pressed & sintered molybdenum	Stress relieved	Retainer plate	The runners & vents are in this plate
1	High-density pressed & sintered molybdenum	Stress relieved	Replacement	--
2	Cb-25% Zr	Stress relieved	Replacement	--
3	GE-474	Stress relieved	Replacement	Received with crack from gate end to ejector pin hole
4	Cu-infiltrated tungsten	Stress relieved	Replacement	--
5	Wrought molybdenum	Stress relieved	Original	--
6	H-13	Tempered to R_C 44-48	Original	-

Table XXX-Tensile Properties
of Copper-Infiltrated Tungsten

Temperature (°F)	UTS (psi)	0.2% YS (psi)	% Elongation
RT	40,000-80,000	--	nil
500	10,000-32,000	--	nil
1000	21,000	--	nil
1500	35,000	28,000-32,000	1-4

Table XXXI--Tensile Properties of
Molybdenum Infiltrated with 2% Beryllium-Copper

Temperature (°F)	UTS (psi)	0.2% YS (psi)	% Elongation
500	55,000	37,000	5

The following test conditions were established for the operation of the materials evaluation die:

Metal Temperature	1500°C-1600°C
Plunger Speed	12-17 ips
Pressure on Metal	8400 psi
Die Temperature	400°C ± 50°C
Dwell Time	2 seconds

Unfortunately, it was found very difficult to maintain the die temperatures within the desired range and at the same time attain high production. The original conditions, therefore, were compromised to a degree in the interest production. Specifically, the dies were permitted to operate in the temperature range of 400°F to 650°F, rather than 400°F ± 50°F. Even this relaxation of the standards, however, did not eliminate the problem of overheating. During periods of high production (up to four shots per minute), it was found necessary to periodically stop casting to permit the dies to cool. It was obvious that more effective cooling was required.

In addition to the obvious advantages of high production rates, it was also observed that high production rates dramatically reduced the slag-control problem. High production rates can also be expected to reduce problems related to changes in the chemical composition of the melt with time.

Figures 114 and 115 illustrate the surface quality attained in the first AISI 304 stainless steel castings produced in the materials evaluation die.

After 255 shots on the evaluation die, the high-density, pressed and sintered molybdenum insert, in ejector half Position 1, was removed to make room for a replacement-style copper-infiltrated molybdenum insert which had been stress relief annealed for 1/2 hour at 700°C in vacuum; and the Cb-25% zirconium insert, in ejector half Position 2, was removed to make room for a replacement-style HOT SHOT 2920X insert that had also been stress relief annealed. Both of the used inserts were in excellent condition when they were removed from the die.

In order to discriminate between the several die materials being evaluated, to observe the progressive effect of service on their condition, and to determine the eventual mode of failure of each material, the dies themselves were examined periodically in minute detail. Many of the defects noted in the die were not detectable in the castings, and with the exception of those inserts specifically identified as being unacceptable, all of the inserts continued to produce castings of acceptable to superior quality.

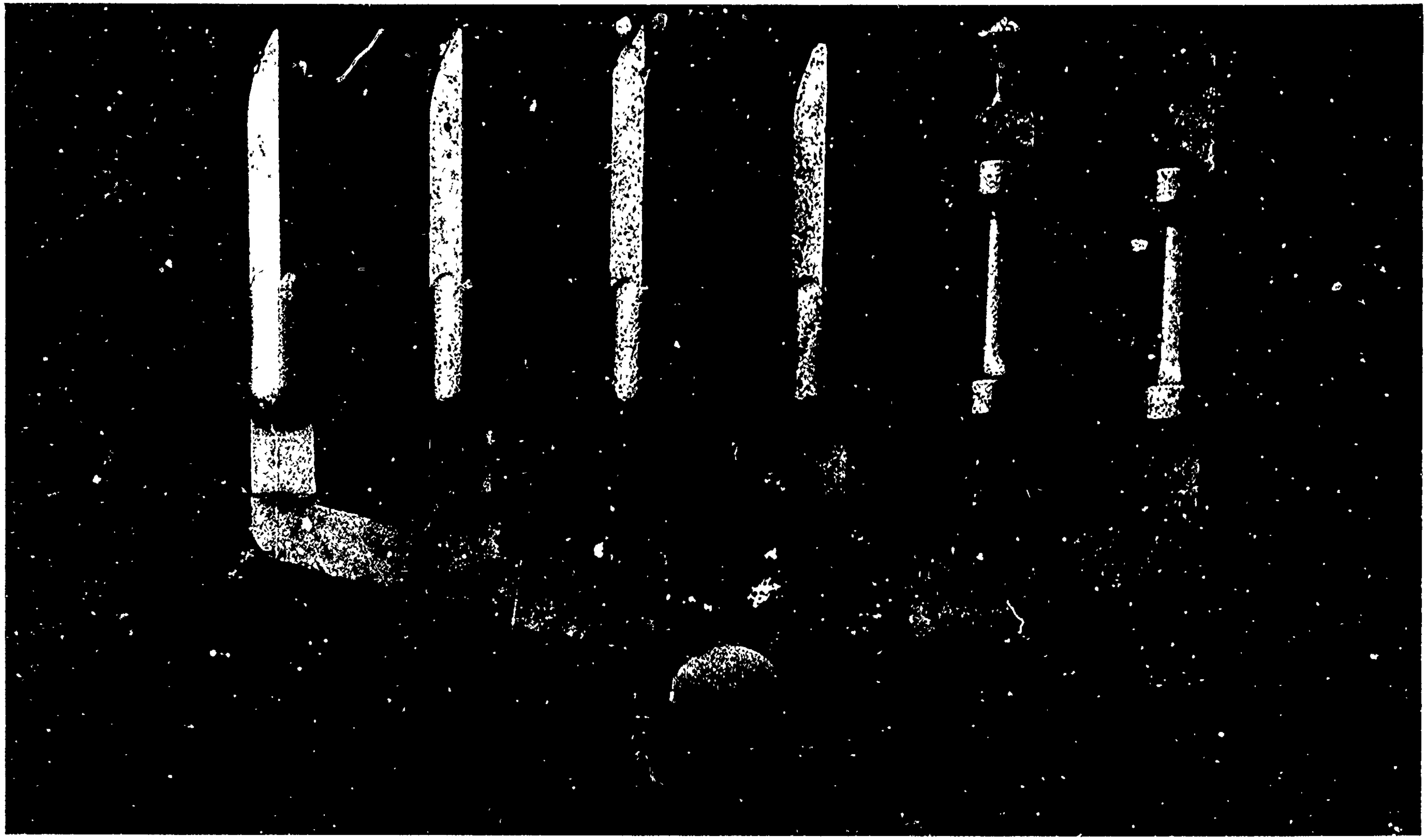
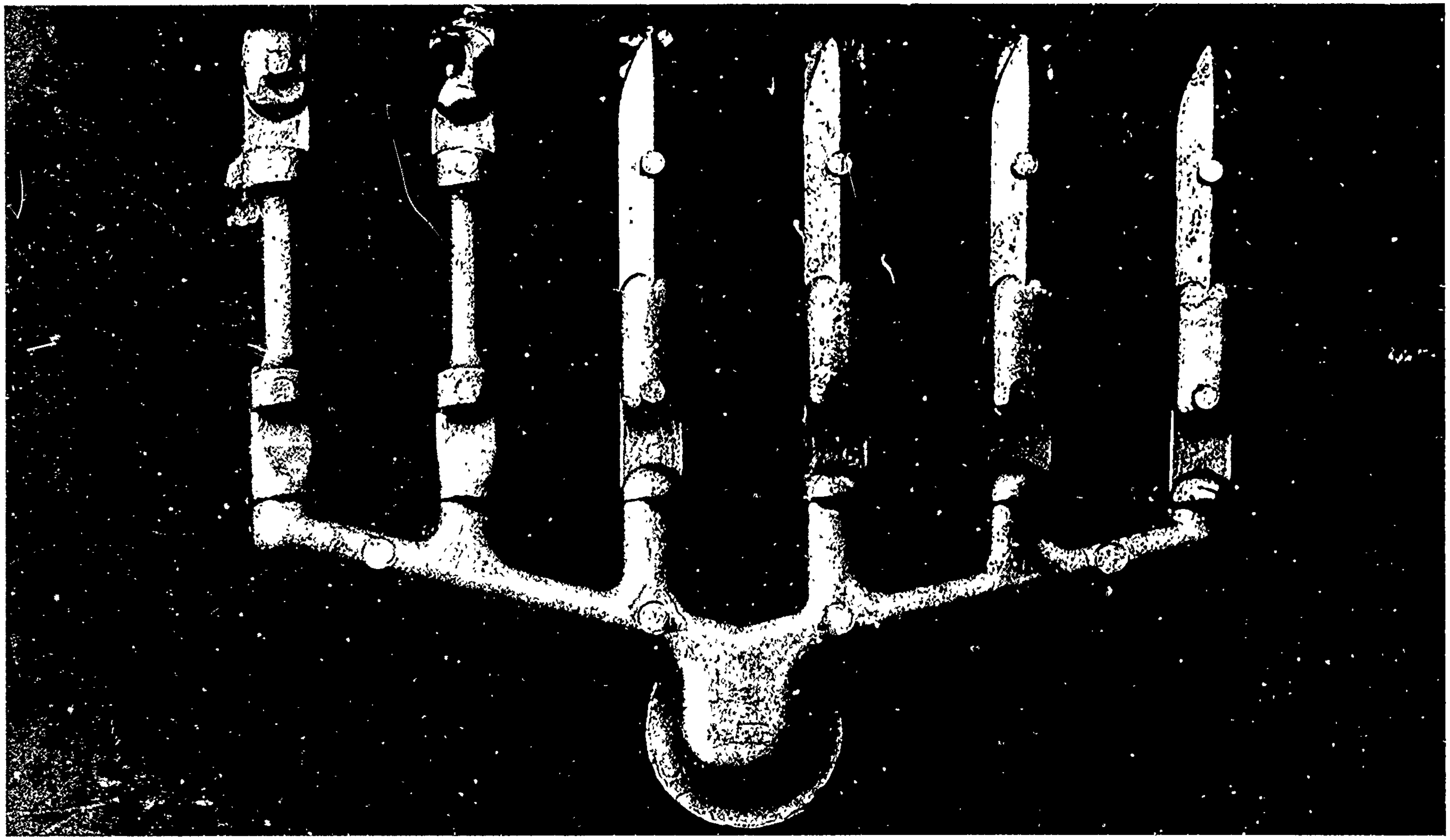


Figure 114 - Cover side view of Shot 4 made in the materials evaluation die from AISI 304 stainless steel. Note, the flash between the insert retainer plate and the inserts, and between the insert retainer plate and the shot sleeve liner, and the limited flash on the parting face. The biscuit was cut shot to facilitate handling, revealing centerline shrinkage.



<u>Figure 115</u> - Ejector face view of Shot 4 made in the materials evaluation die from AISI 304 stainless steel. Note the flash between the insert retainer plate and the inserts and the limited flash on the parting face.

After 728 shots, the die was removed for a detailed inspection. The results of that inspection are summarized in Tables XXXII and XXXIII. Before resuming operation, the H-13 insert was removed from the die and photographed. (See Figure 116.) It was judged to be close to the end of its useful life. A second HOT SHOT 2920X insert, solution annealed and later stress relief annealed, was substituted for the H-13 in ejector half Position 6.

The die was not again removed for a detailed inspection until Shot 3000. Figures 117 and 118 indicate the condition of the die at that point. The results of the visual inspection are summarized in Tables XXXIV and XXXV.

At that point, the copper-infiltrated molybdenum insert, in ejector half Position 1, was replaced with a stress relieved Cb-752 insert. The copper-infiltrated molybdenum insert had deteriorated badly, as illustrated by Figure 119, although evidence of this deterioration had not been noted in the casting until sometime after Shot 2450 (2195 shots on the insert).

Having made the change noted above, the die was put back into service and re-examined, only after reaching Shot 7017. Figures 120 and 121 illustrate the condition of the die after 7017 shots. Tables XXXVI and XXXVII summarize the results of the visual inspection made of the die.

Before putting the die back into service, the GE-474 insert, in ejector half Position 4, which had been exposed to 7017 shots, was removed. As indicated by Figure 122, high quality castings could no longer be produced in the GE-474 insert. It was replaced by the high-density, pressed and sintered molybdenum insert, which had previously experienced 255 shots in ejector half Position 1. The Cb-752 insert, in ejector half Position 6, was also removed after Shot 7017 (4017 shots on the insert). It, too, was producing castings with an unacceptable surface quality. (See Figure 123.) The replacement-style Cb-25% zirconium insert, previously exposed to 255 shots in ejector half Position 2, was reinserted in place of the Cb-752.

Another aspect of die life that was carefully checked after 7017 shots on the materials evaluation die was the wear experienced by the ejector pin holes. The data generated are compiled in Table XXXVIII. Although these data are contaminated by a number of operational irregularities, they are felt to be indicative of normal wear expectations when using wrought molybdenum ejector pins. Some of the irregularities include: the evaluation of high-density, pressed and sintered molybdenum pins in some of the holes, and several experimental refractory metal alloys in others; the occasional expedient of plugging one or more ejector pin holes from time to time to minimize downtime; occasional, continued operation with bent ejector pins, either unwittingly or as an expedient to avoid interruption of the operation; and the evaluation of a variety of ejector pin lubricants (none of which, in the final analysis, could be distinguished from another.) Subsequent to the gathering of the data reported in Table XXXVIII, it was also discovered that some of the ejector pin holes in the replacement style inserts may not have been absolutely perpendicular to the ejector plate.

Table XXXII--Results of Inspection of the Materials Evaluation Die After 728 Shots

Position	Material	Condition	Insert Style	No. of Shots	Remarks
Cover half	High-density pressed & sintered molybdenum	Stress relieved	Retainer plate	728	Raised replica of runner system formed on originally flat surface
1	Anviloy 1150	Stress relieved	Original	728	Area opposite gate heat checked but not raised, area opposite vent not raised, gate end of insert sharp and straight
2	High-density pressed & sintered molybdenum	Stress relieved	Original	728	Area opposite gate and vent raised, edge at gate end of cavity rounded, gate end of insert deformed but edge sharp
3	Mo-3	Stress relieved	Replacement	728	Area opposite gate raised, edges at gate end of cavity and gate end of insert rounded, gate end of insert deformed, hammer marks noted
4	80-20	Stress relieved	Replacement	728	Nicks and dents noted in cavity, edge at gate end of cavity rounded perhaps from abuse, gate end of insert deformed but edge sharp, areas opposite gate and vent not raised
5	TZM	Stress relieved	Replacement	728	Only area opposite gate raised very slightly, all edges sharp, no deformation
6	High-density pressed & sintered molybdenum	As machined	Replacement	728	Areas opposite gate and vent raised, gate end of insert deformed but edge sharp, edge at gate end of cavity rounded

Table XXXII--Results of Inspection of the Materials Evaluation Die After 728 Shots
(Continued)

Position	Material	Condition	Insert Style	No. of Shots	Remarks
Ejector half	High-density pressed & sintered molybdenum	Stress relieved	Replacement	728	The flat surface opposite the tip of the recessed shot sleeve liner is raised, bottom of sprue cavity dented by plunger, area around sprue badly nicked and dented, flash welded to face of plate on the upper right of Pocket 6 and at 2, 9, and 12 o'clock around the sprue
1	Copper-infiltrated molybdenum	Stress relieved	Replacement	473	Gate end of cavity nicked, gate end of insert is straight with a sharp edge
2	HOT SHOT 2920X	Stress relieved	Replacement	473	Transverse heat checks noted in cavity, gate end of insert is straight with a sharp edge
3	GE-474	Stress relieved	Replacement	728	Round-bottomed, longitudinal heat checks noted in half-round section of cavity
4	Wrought molybdenum	As machined	Replacement	726	Ejector pin difficulties have marred pin holes, use of screw driver to eject castings has badly dented the edge of the gate end of the cavity, parting line dented by hammer on either side of gate, insert cracked along zero-radius section

Table XXXII--Results of Inspection of the Materials Evaluation Die After 728 Shots
(Continued)

Position	Material	Condition	Insert Style	No. of Shots	Remarks
5	Wrought molybdenum	Stress relieved	Original	728	Edges of radiused regions of tensile bar cavity deformed upward, hammer dents noted on right of gate, ejector pin holes and gate end of insert are good
6	H-13	Tempered to R_C 44-48	Original	728	Ends of cavity deformed into protruding lips which appear eroded, gate and cavity heat checked, gate end of insert deformed and edge rounded, ejector pin holes are okay, insert removed as unsatisfactory

Table XXXIII--Measurement of the Gaps Formed Between the Insert Retainer Plates and the Gate End of the Inserts After 728 Shots

Cover Half

Position	Gap (in)	Insert Material
1	0.029	Anviloy 1150
2	0.025	Pressed & sintered Mo
3	0.035	Mo-3
4	0.037	80-20
5	0.035	TZM
6	0.027	Pressed & sintered Mo

Ejector Half

Position	Gap (in)	Insert Material
1	0.037	Pressed & sintered Mo--255 shots Cu-infiltrated Mo--473 shots
2	0.025	Cb-25%.Zr--255 shots HOT SHOT 2920X--473 shots
3	0.037	GE-474
4	0.035	Cu-infiltrated tungsten--2 shots Wrought Mo--726 shots
5	0.022	Wrought Mo
6	0.043	H-13

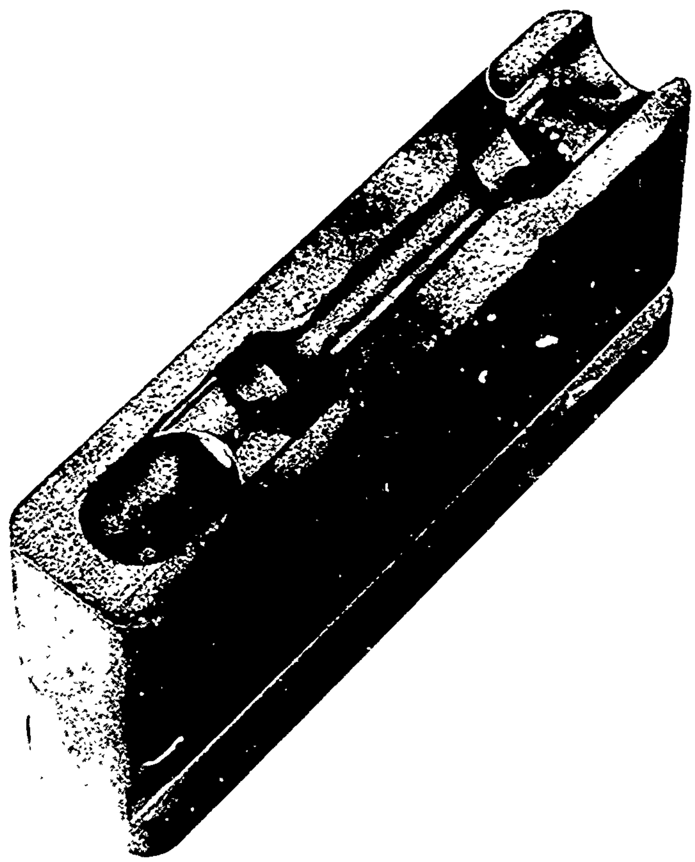


Figure 116 - H-13 steel insert removed from the materials evaluation die after 728 shots.

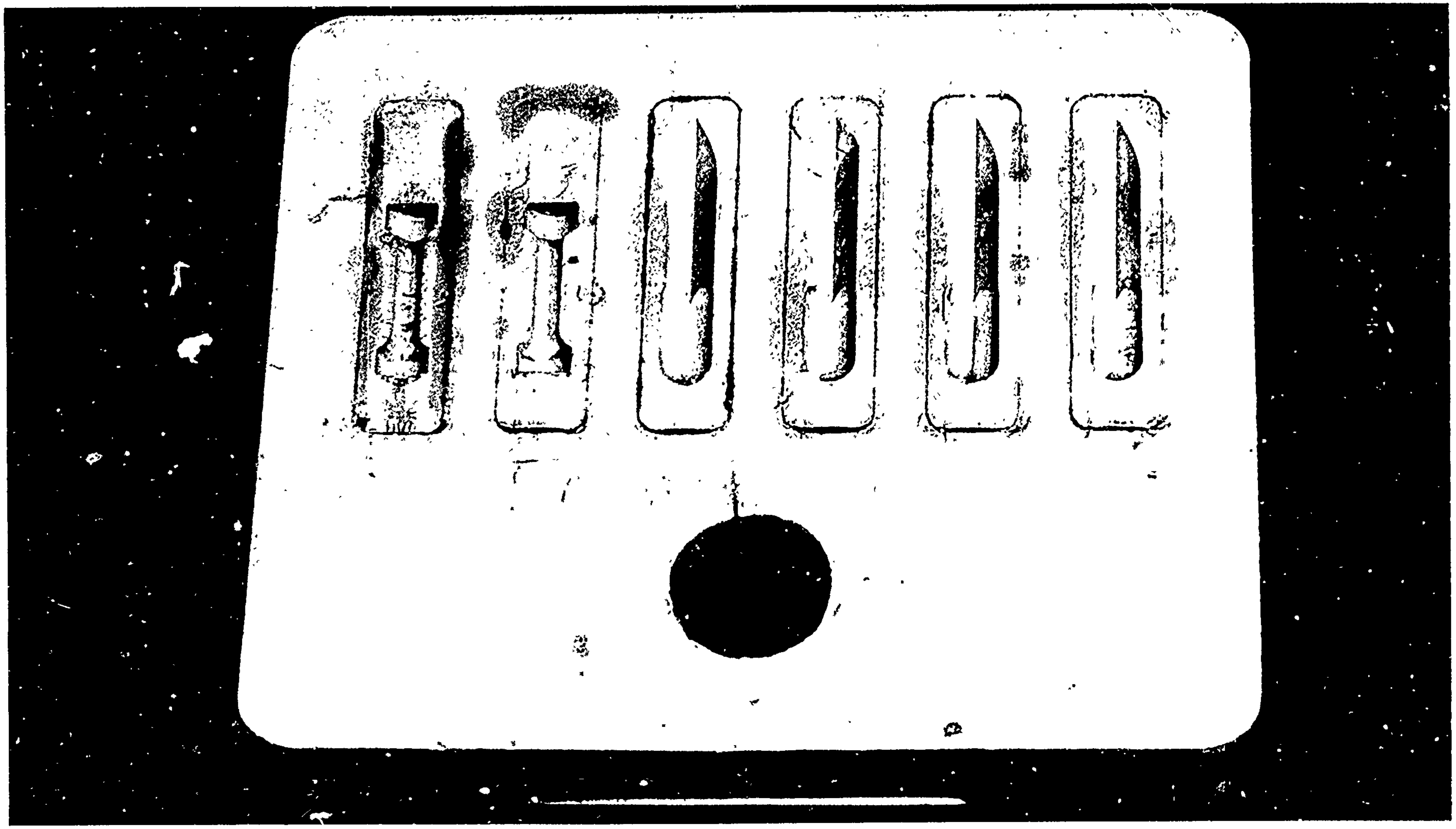


Figure 117 - Cover half of the materials evaluation die after 3000 shots. From left to right, the inserts are: Anviloy 1150--3000 shots; pressed and sintered molybdenum--3000 shots; Mo-3--3000 shots; 80-20--3000 shots; TZM--3000 shots; pressed and sintered molybdenum--3000 shots.

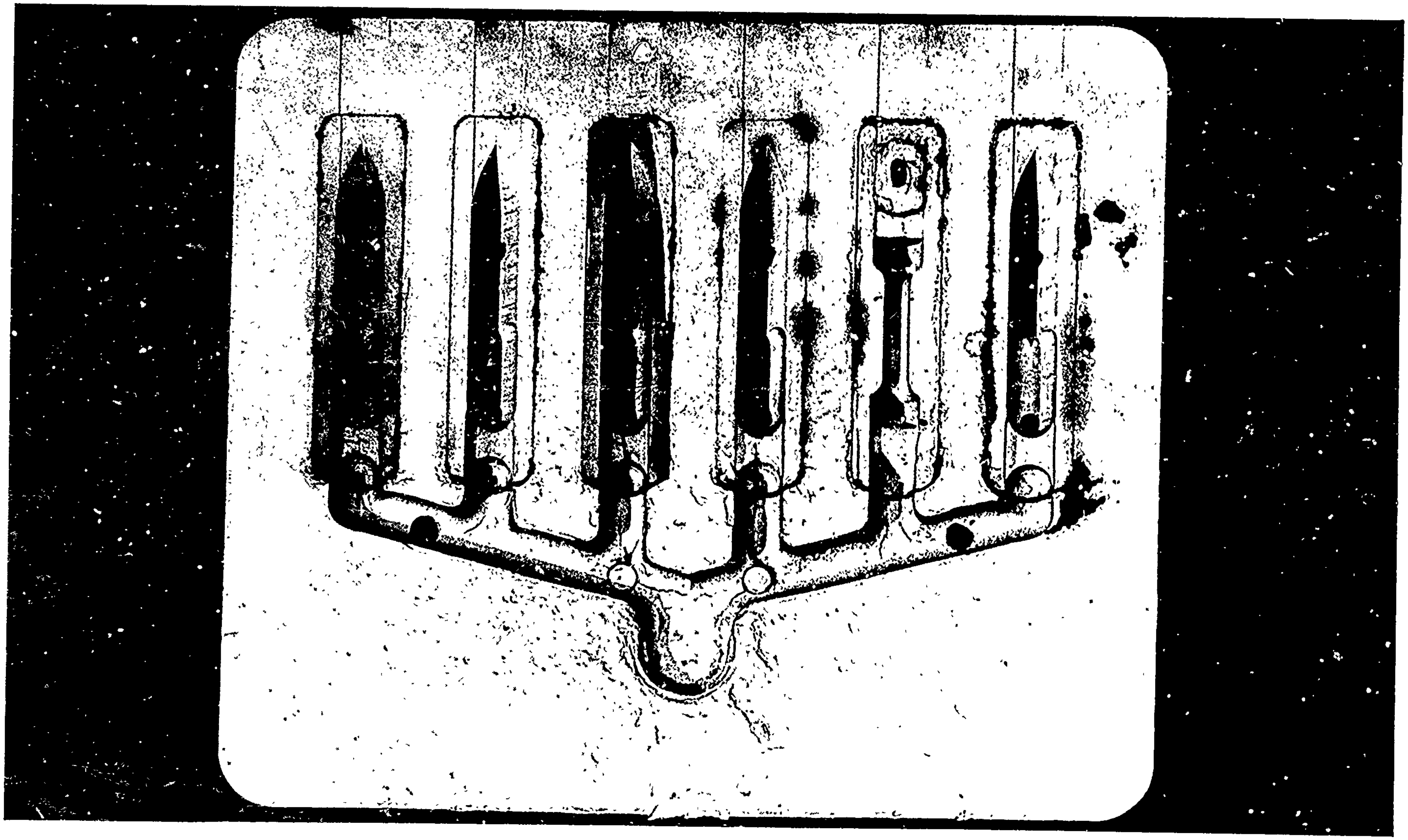


Figure 118 - Ejector half of the materials evaluation die after 3000 shots. From left to right, the inserts are: copper-infiltrated molybdenum--2745 shots; HOT SHOT 2920X--2745 shots; GE-474--3000 shots; wrought molybdenum--2998 shots; wrought molybdenum--3000 shots; HOT SHOT 2920X--2272 shots.

Table XXXIV--Results of Inspection of the Materials Evaluation Die After 3000 Shots

Position	Material	Condition	Insert Style	No. of Shots	Remarks
Cover half	High-density pressed & sintered molybdenum	Stress relieved	Retainer plate	3000	Raised replica of runner system decorated with predominantly transverse heat checks densely distributed near sprue, tight crack from lower right corner of Pocket 3 through the shot sleeve hole to the lower edge of plate is brittle failure due to careless handling, flash at 7 o'clock to the shot sleeve hole has penetrated to within 1/4" of the edge of the plate
1	Anviloy 1150	Stress relieved	Original	3000	Heat checking has spread to include all areas but the vent end exposed to metal by opposing replacement style insert, gate-end edge of cavity eroded, edges of radiused sections of cavity slightly raised
2	High-density pressed & sintered molybdenum	Stress relieved	Original	3000	No heat checking, edges of cavity raised at gate end and radiused sections, small crack, 1/8" long, from lower left corner of cavity onto parting line, soldering on parting line in two places, flat opposite vent dented
3	Mo-3	Stress relieved	Replacement	3000	Unchanged from previous inspection, no heat checking

Table XXXIV--Results of Inspection of the Materials Evaluation Die After 3000 Shots
(Continued)

Position	Material	Condition	Insert Style	No. of Shots	Remarks
4	80-20	Stress relieved	Replacement	3000	Four, generally transverse, cracks noted extending from cavity into parting line, two at either end of the cavity, assumed to be random brittle failure, no heat checking
5	TZM	Stress relieved	Replacement	3000	Longitudinal crack extending from half-round section of cavity to gate end of insert noted, no heat checking
6	High-density pressed & sintered molybdenum	As machined	Replacement	3000	Basically no change, no heat checking, gate-end edge of cavity now appears raised
Ejector half	High-density pressed & sintered molybdenum	Stress relieved	Retainer plate	3000	Wide cracks radiating from plugged ejector pin hole in runners on either side of sprue noted, cracks also noted in the sprue and in the runners to Pockets 2 and 5, flash was noted welded all along the right side of Pocket 6 and to the right of the sprue
1	Copper-infiltrated molybdenum	Stress relieved	Replacement	2745	Badly heat checked and pitted, condition of insert judged unacceptable
2	HOT SHOT 2920X	Stress relieved	Replacement	2745	Heat checked throughout cavity, checks are predominantly transverse in the triangular section, form typical network in the half-round section, fine pitting or corrosion noted in gate

Table XXXIV--Results of Inspection of the Materials Evaluation Die After 3000 Shots
(Continued)

Position	Material	Condition	Insert Style	No. of Shots	Remarks
3	GE-474	Stress relieved	Replacement	3000	Heat checks in half-rounded section form typical network, appear rounded by erosion, initial crack is eroded and paralleled by many short eroded checks, short tight transverse crack runs through upper ejector pin hole
4	Wrought molybdenum	As machined	Replacement	2998	Longitudinal crack runs length of cavity along the apex of the triangular section, through the half-round section, all the way to the gate end of the insert, no heat checking noted
5	Wrought molybdenum	Stress relieved	Original	3000	Longitudinal crack noted in cavity running from one ejector pin hole to the other, no heat checking noted
6	HOT SHOT 2920X	Solution annealed and stress releived	Replacement	2272	Small number of heat checks noted in cavity, transverse only, lower left corner of insert broken away

Table XXXV--Measurement of the Gaps Formed Between the Insert Retainer Plates and the Gate End of the Inserts After 3000 Shots

Cover Half

Position	Gap (in)	Insert Material
1	0.041	Anviloy 1150
2	0.041	Pressed & sintered Mo
3	0.051	Mo-3
4	0.051	80-20
5	0.048	TZM
6	0.041	Pressed & sintered Mo

Ejector Half

Position	Gap (in)	Insert Material
1	0.049	Pressed & sintered Mo--255 shots Cu-infiltrated Mo--2745 shots
2	0.032	Cb-25% Zr--255 shots HOT SHOT 2920X--2745 shots
3	0.038	GE-474
4	0.038	Cu-infiltrated tungsten--2 shots Wrought Mo--2998 shots
5	0.031	Wrought Mo
6	0.038	H-13--728 shots HOT SHOT 2920X--2272 shots

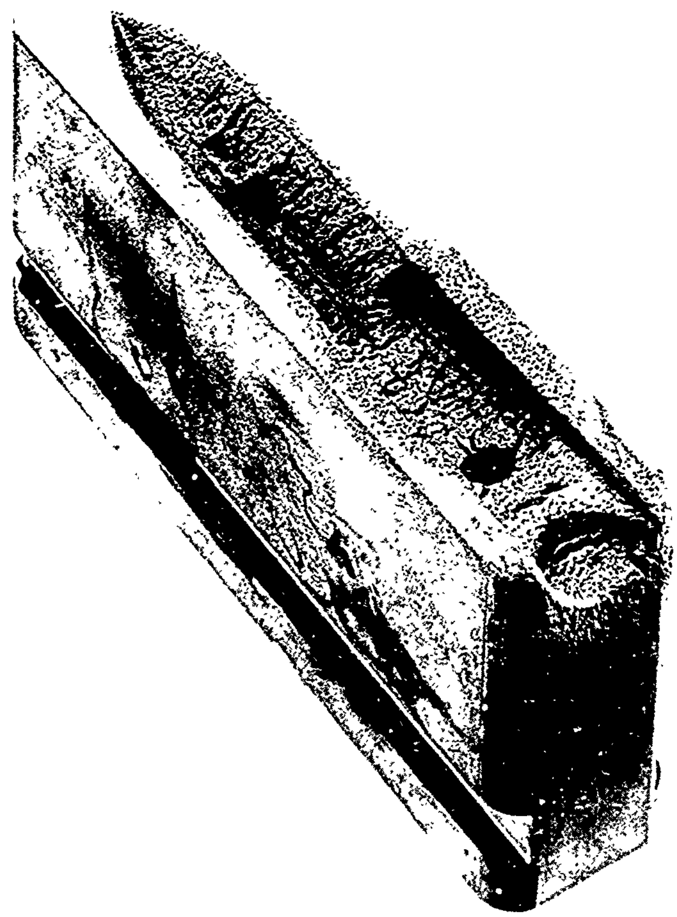
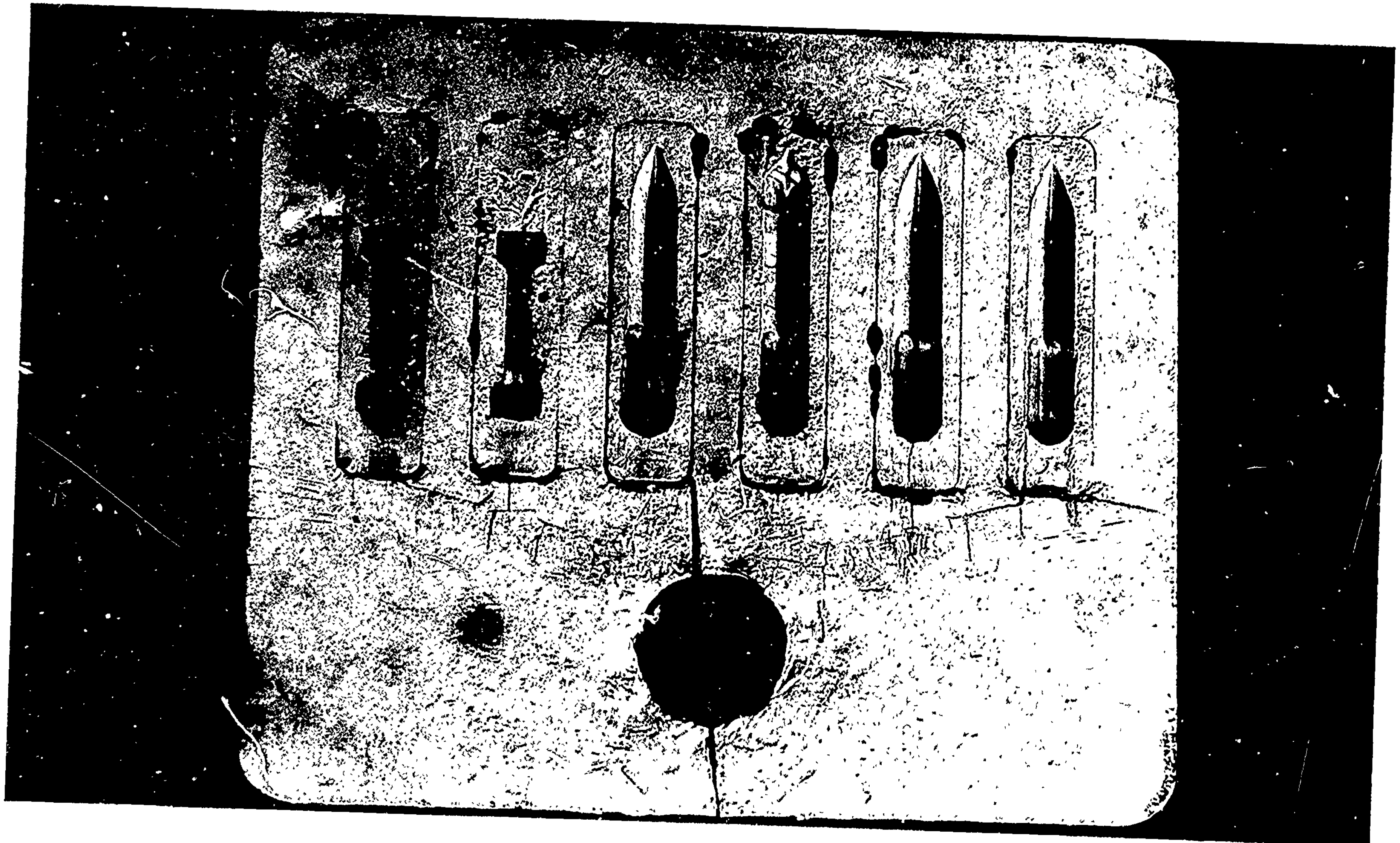


Figure 119 - Copper-infiltrated molybdenum insert (2% beryllium-copper) removed from the materials evaluation die after experiencing 2745 shots.



<u>Figure 120</u> - Cover half of the materials evaluation die after 7017 shots. From left to right, the inserts are: Anviloy 1150--7017 shots; pressed and sintered molybdenum--7017 shots; Mo-3--7017 shots; 80-20--7017 shots; TZM--7017 shots; pressed and sintered molybdenum--7017 shots.

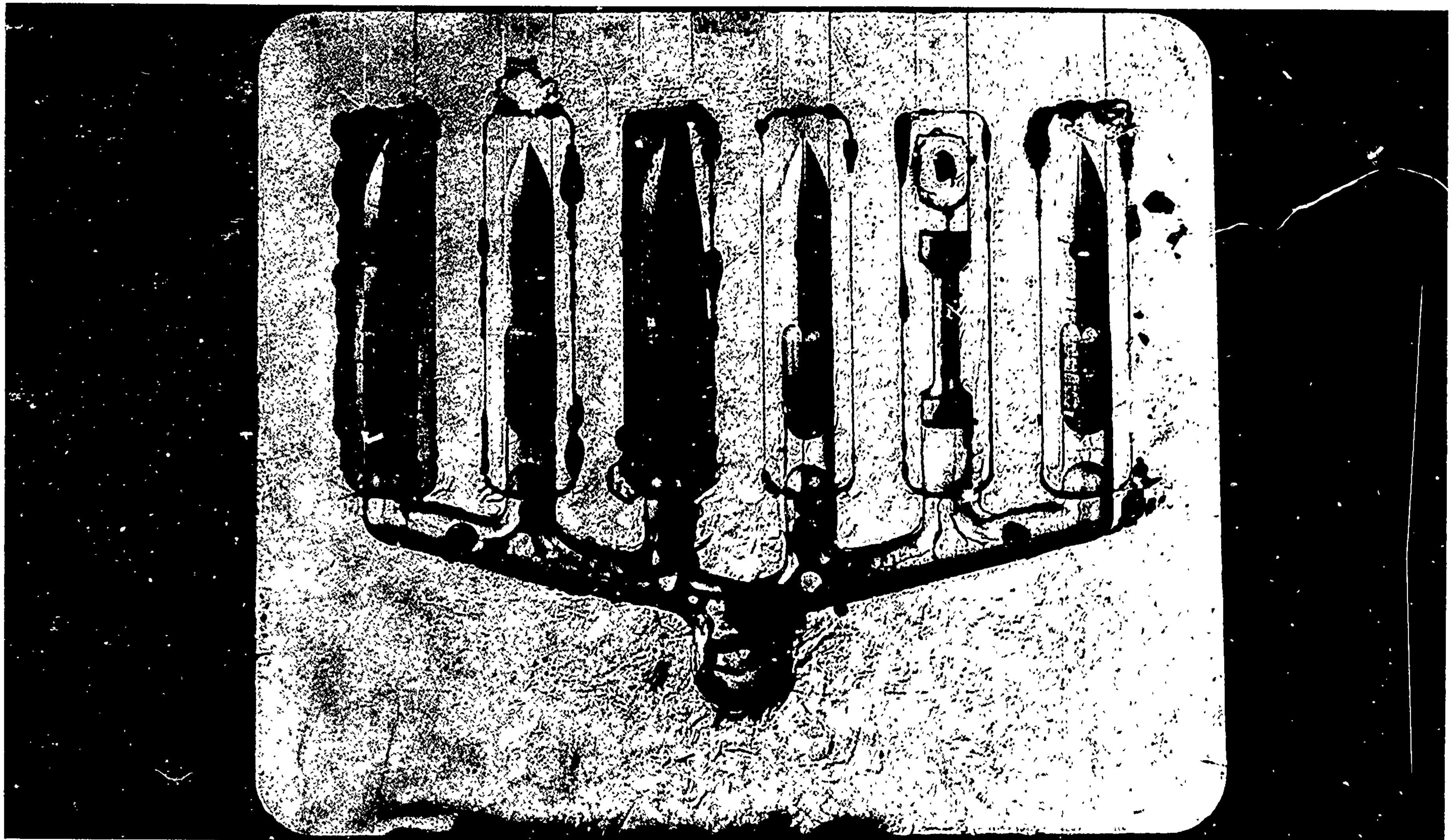


Figure 121 - Ejector half of the materials evaluation die after 7017 shots. From left to right, the inserts are: Cb-752--4017 shots; HOT SHOT 2920X--6762 shots; GE-474--7017 shots; wrought molybdenum--7015 shots; wrought molybdenum--7017 shots; HOT SHOT 2920X--6289 shots.

Table XXXVI--Results of Inspection of the Materials Evaluation Die After 7017 Shots

Position	Material	Condition	Insert Style	No. of Shots	Remarks
Cover half	High-density pressed & sintered molybdenum	Stress relieved	Retainer plate	7017	Crack from Pocket 6 to bottom of plate has widened, transverse cracks have widened too, those below Pockets 2 and 5 have run up vertical sections of runners into pockets, close to the sprue a network of heat checks is forming
1	Anviloy 1150	Stress relieved	Original	7017	Heat checking has grown worse, edges of radiused section of cavity nearer gate and edges of reduced section of cavity have disintegrated
2	High-density pressed & sintered molybdenum	Stress relieved	Original	7017	Edges of cavity rolled back at gate end and in radiused section at vent end, cracks noted in the circular grooves where the radiused sections join the shoulders and at the gate end of the cavity
3	Mo-3	Stress relieved	Replacement	7017	Crack noted in bottom of half-round section only demonstrates small scale irregularities unlike straight heat checks and delaminations, pitting also noted in this section. Edges of half-round section of cavity near gate appear peened or pulled back
4	80-20	Stress relieved	Replacement	7017	Heat checking noted in lower end of triangular section and in half-round section of cavity and on gate-end edge of cavity

Table XXXVI--Results of Inspection of the Materials Evaluation Die After 7017 Shots
(Continued)

Position	Material	Condition	Insert Style	No. of Shots	Remarks
5	TZM	Stress relieved	Replacement	7017	Unchanged from previous inspection, no heat checking noted
6	High-density pressed & sintered molybdenum	Stress relieved	Replacement	7017	Edge of half-round section of cavity appears peened back at gate end and along left side, no heat checking noted
Ejector half	High-density pressed & sintered molybdenum	Stress relieved	Retainer plate	7017	All cracks have become enlarged, those nearer sprue are interconnected, cracks now also noted in vertical segments of runners leading to Pockets 1 and 6
1	Cb-752	Stress relieved	Replacement	4017	Scattered heat checks, mostly transverse, were noted to be wide and deep and more dense in center section where the die maker's error had created a greater cross section, judged unsatisfactory
2	HOT SHOT 2920X	Stress relieved	Replacement	6762	Heat checks more pronounced and profuse, extend from the half-round section of the cavity into the parting line, erosion more noticeable in gate
3	GE-474	Stress relieved	Replacement	7017	Heat checking and erosion have become unacceptable
4	Wrought molybdenum	As machined	Replacement	7015	Unchanged from previous inspection, no heat checking noted

Table XXXVI--Results of Inspection of the Materials Evaluation Die After 7017 Shots
(Continued)

Position	Material	Condition	Insert Style	No. of Shots	Remarks
5	Wrought molybdenum	Stress relieved	Original	7017	Longitudinal crack has extended into overflow, short longitudinal cracks noted on both sides of ingate, edges of radiused sections at both ends of cavity appear rolled back slightly
6	HOT SHOT 2920X	Solution annealed and stress relieved	Replacement	6289	Heat checks in half-round section of cavity only, now form typical network, heat checking noted in runner and cavity edges of ingate, some chipping on cavity edge of gate

Table XXXVII--Measurement of the Gap Formed Between the Insert Retainer Plates and the Gate End of the Inserts After 7017 Shots

Cover Half

Position	Gap (in)	Insert Material
1	0.040-0.050	Anviloy 1150
2	0.050-0.060	Pressed & sintered Mo
3	0.060-0.070	Mo-3
4	0.060-0.070	80-20
5	0.050-0.060	TZM
6	0.040-0.050	Pressed & sintered Mo

Ejector Half

Position	Gap (in)	Insert Material
1	Not recorded	Pressed & sintered Mo--255 shots Cu-infiltrated Mo--2745 shots Cb-752--4017 shots
2	--	Cb-25% Zr--255 shots HOT SHOT 2920X--6762 shots
3	--	GE-474
4	--	Cu-infiltrated tungsten--2 shots Wrought Mo--7015 shots
5	--	Wrought Mo
6	--	H-13--728 shots HOT SHOT 2920X--6289 shots

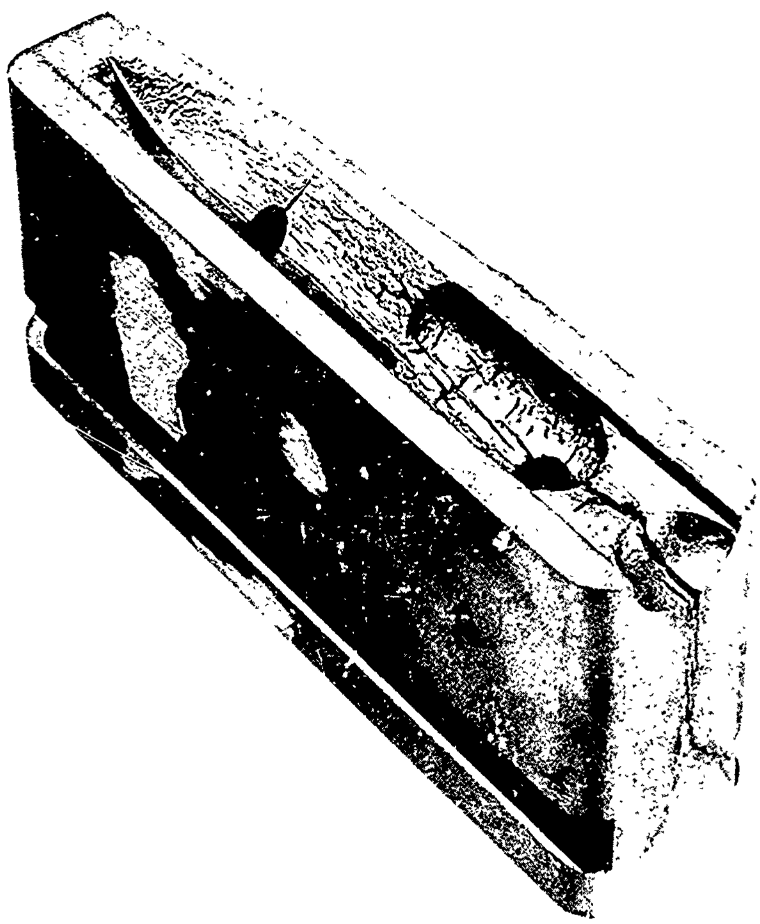


Figure 122 - GE-474 insert removed from the materials evaluation die after 7017 shots. The white areas are remnants of the molybdenum can in which the alloy was extruded. The gate end of the insert was cracked as received, before being exposed to liquid metal.

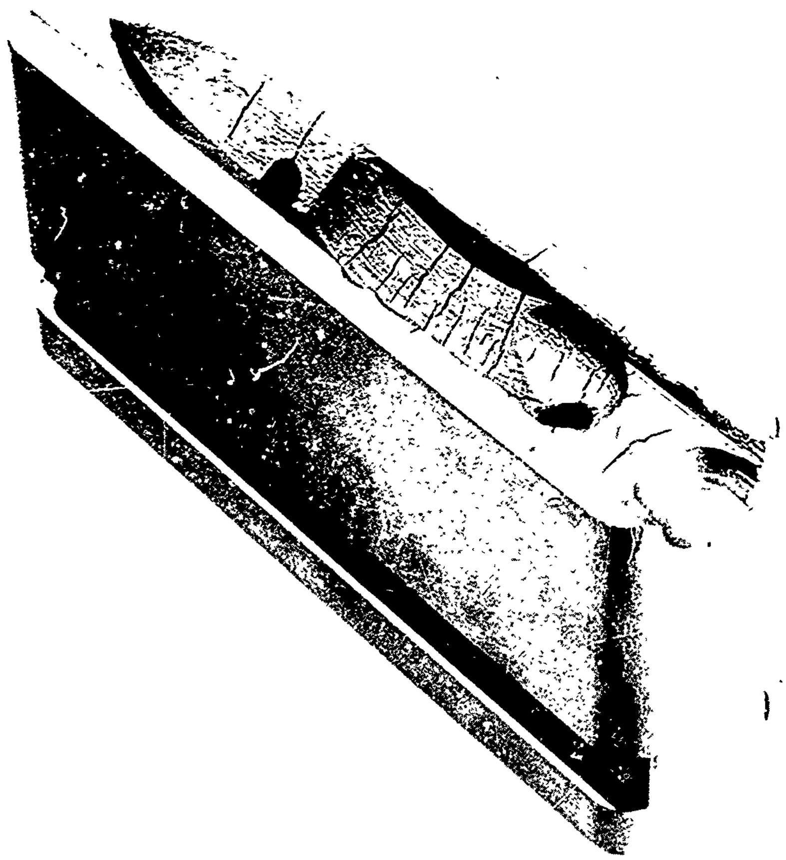


Figure 123 - Cb-752 insert removed from the materials evaluation die after experiencing 4017 shots.

Table XXXVIII--Diameters of the Ejector Pin Holes in the Materials Evaluation Die After 7017 Shots

Component	Material	Condition	No. of Shots	Position	Diameter (in)	Original Diameter (in)
Insert retainer plate	High-density pressed & sintered molybdenum	Stress relieved	7017	1	0.252-0.254	0.250 -0.251
				2	plugged	0.250 -0.251
				3	plugged	0.250 -0.251
				4	0.256-0.259	0.250 -0.251
Insert #1 replacement	Cb-752	Stress relieved	4017	Lower	0.193-0.194	0.1875-0.1885
				Upper	0.188-0.189	0.1875-0.1885
Insert #2 replacement	HOT SHOT 2920X	Stress relieved at 800°C	6762	Lower	0.188-0.189	0.1875-0.1885
				Upper	0.187-0.188	0.1875-0.1885
Insert #3 replacement	GE-474	Stress relieved	7017	Lower	0.189-0.190	0.1875-0.1885
				Upper	0.190-0.191	0.1875-0.1885
Insert #4 replacement	Wrought molybdenum	As machined	7015	Lower	0.195-0.196	0.1875-0.1885
				Upper	0.195-0.200	0.1875-0.1885
Insert #5 original	Wrought molybdenum	stress relieved	7017	Lower	0.195	0.1875-0.1885
				Upper	0.193-0.197	0.1875-0.1885
				Overflow	0.190-0.191	0.1875-0.1885
Insert #6 replacement	HOT SHOT 2920X	Solution annealed & stress relieved	6289	Lower	0.188-0.190	0.1875-0.1885
				Upper	0.189-0.194	0.1875-0.1885

The difficulties experienced with the necking down of shot sleeve liners were described in Section III. Associated with this problem was an enlargement of the hole bored in the cover half insert retainer plate to accommodate the tip of the shot sleeve liner. That hole was observed to have become enlarged from an original diameter of 1.505" to 1.507" to a diameter of 1.526" to 1.546" after 7017 shots.

The materials evaluation die was put back into service, in spite of the difficulties with the injection system, with the intention of taking corrective action at the earliest opportunity.

In an effort to solve the injection system problem, a 2-1/2" diameter by 3/4" long bushing was fabricated from a 3" diameter wrought molybdenum bar to accept the tips of the shot sleeve liners. It was inserted in the cover half of the materials evaluation die, following Shot 7442. It brought about an immediate, but short-lived, improvement in the production rate. After 1550 additional shots, however, the inner diameter of that bushing had increased from 1.505" to 1.507" to a diameter of 1.514" to 1.528". The bushing can be seen in Figure 124.

During the same time period, the Lamp Metals and Components Department experienced a failure of the refractories in their 300 pound induction melting furnace. As usual in such circumstances, the induction coil was perforated.

Shortly thereafter, a link pin in the lock-up mechanism of the die casting machine broke, permitting the die to flash spectacularly. At that point, 8992 castings had been made. The dies were removed from the die casting machine for inspection. The results of that inspection are summarized in Table IXL. Photographs were made to illustrate the condition of the die. They appear as Figures 124 and 125. Table XL records the measurements made of the gaps formed between the insert retainer plates and the inserts, and between the cover half insert retainer plate and the shot sleeve bushing. Table XLI records the diameters of the ejector pin holes.

Both of the high-density, pressed and sintered molybdenum insert retainer plates were judged to be unsatisfactory for further service after 8992 shots. All of the inserts were pressed out of the retainer plates. The Anviloy 1150 insert in cover Impression 1, and the 80-20 insert in cover Impression 4, were also judged to be unacceptable. Figure 126 illustrates the condition of the Anviloy 1150 insert. When the 80-20 insert was removed from the retainer plate, a large piece of material, bounded on one side by one of the four cracks previously noted, broke away from the upper left-hand corner of the insert parting face. The condition of the 80-20 insert is illustrated by Figure 127. When the as-machined, wrought molybdenum, replacement- style insert was pressed from ejector Impression 4, it cleaved longitudinally into two pieces along the delamination which had been evident for so many shots. Examination of the fracture revealed that the entire surface was oxidized except a small area adjacent to the support plate at the vent end of the insert. It was concluded that the portion of the fracture surface that was oxidized had formed in service. The unoxidized surface formed by propogation of the existing crack during removal of the insert. Figure 128 illustrates the condition of the insert which was no longer useful--although it was completely free from heat checking.

Table IXL--Results of Inspection of the Materials Evaluation Die After 8992 Shots

Position	Material	Condition	Insert Style	No. of Shots	Remarks
Cover half	High-density pressed & sintered molybdenum	Stress relieved	Retainer plate	8992	The vertical cracks in the runner system are all enlarged, additional vertical cracking noted in the runner leading to Pocket 1, heat checked network has spread out, away from sprue, gaps have opened between outside diameter of bushing inserted after 7442 shots and plate, sinusoidal channel cut from Pocket 1 to left edge of plate by flash, retainer plate judged unsatisfactory.
1	Anviloy 1150	Stress relieved	Original	8992	Erosion of gate end has produced 13/64" radius, disintegration of the edges of the reduced section of the cavity have produced 1/8" radii, erosion also noted on edge at vent end of cavity, flash welded to parting line at the left of the gate-end shoulder, condition of insert judged unsatisfactory.
2	High-density pressed & sintered molybdenum	Stress relieved	Original	8992	Little changed from 7017 shots, left edge of reduced section of cavity appears peened, slight indication of incipient pitting or heat checking in gate-end shoulder, radiused section and reduced section

Table IXL--Results of Inspection of the Materials Evaluation Die After 8992 Shots
(Continued)

Position	Material	Condition	Insert Style	No. of Shots	Remarks
3	Mo-3	Stress relieved	Replacement	8992	Little changed from 7017 shots, pitting in half-round section has produced noticable roughening of the surface.
4	80-20	Stress relieved	Replacement	8992	Cracks have continued to enlarge, heat checks now noted on left edge of half-round section of cavity, additional denting noted on parting line to the left of the triangular section, insert judged ready for replacement.
5	TZM	Stress relieved	Replacement	8992	Longitudinal crack has propogated to vent end of cavity but lies to the right of the small-radius groove, incipient pitting noted in half-round section, otherwise little changed.
6	High-density pressed & sintered molybdenum	Stress relieved	Replacement	8992	Tiny heat checks noted in edge of cavity at gate end and on left side of the half-round section, also in the cavity on the left side of the half-round section and on the right side of the triangular section, incipient pitting noted in the half-round section

Table IXL--Results of Inspection of the Materials Evaluation Die After 8992 Shots
(Continued)

Position	Material	Condition	Insert Style	No. of Shots	Remarks
Ejector half	High-density pressed & sintered molybdenum	Stress relieved	Insert retainer plate	8992	Large cracks have begun to develop around working ejector pin holes, gaping ruptures have developed on the inboard side of the vertical runners to Pockets 1 and 6, a sinusoidal channel was cut from the right side of Pocket 6 to the right edge of the plate by flash.
1	High-density pressed & sintered molybdenum	Stress relieved	Replacement	2230	A number of nicks were noted in and beside the half-round section of the cavity to the left of lower ejector pin, lips have been raised around both ejector pin holes, no heat checking noted.
2	HOT SHOT 2920X	Stress relieved	Replacement	8737	Little changed from 7017 shots
3	Cb-25% Zr	Stress relieved	Replacement	2230	Fine network of heat checks noted in the half-round section and beginning to develop in the triangular section, fine transverse checks noted through each ejector pin hole.
4	Wrought molybdenum	As machined	Replacement	8990	Longitudinal crack has opened noticeably in the runner extension, gate, gate end of the cavity and vent, insert has suffered many new nicks, no heat checks noted.

Table IXL--Results of Inspection of the Materials Evaluation Die After 8992 Shots
(Continued)

Position	Material	Condition	Insert Style	No. of Shots	Remarks
5	Wrought molybdenum	Stress relieved	Original	8992	Central longitudinal crack has extended through the overflow to the vent end of the insert and has opened noticeably in the gate and the runner extension, the edge of the cavity has been drawn back at the gate end, and a crack has started in the groove formed by the gate end of the cavity, no heat checks noted.
6	HOT SHOT 2920X	Solution annealed and stress relieved	Replacement	8264	Little changed from 7017 shots, flash is welded to the parting line on the lower right side of the half-round section, flash has cut a channel in the parting line on the upper right side of the triangular section.

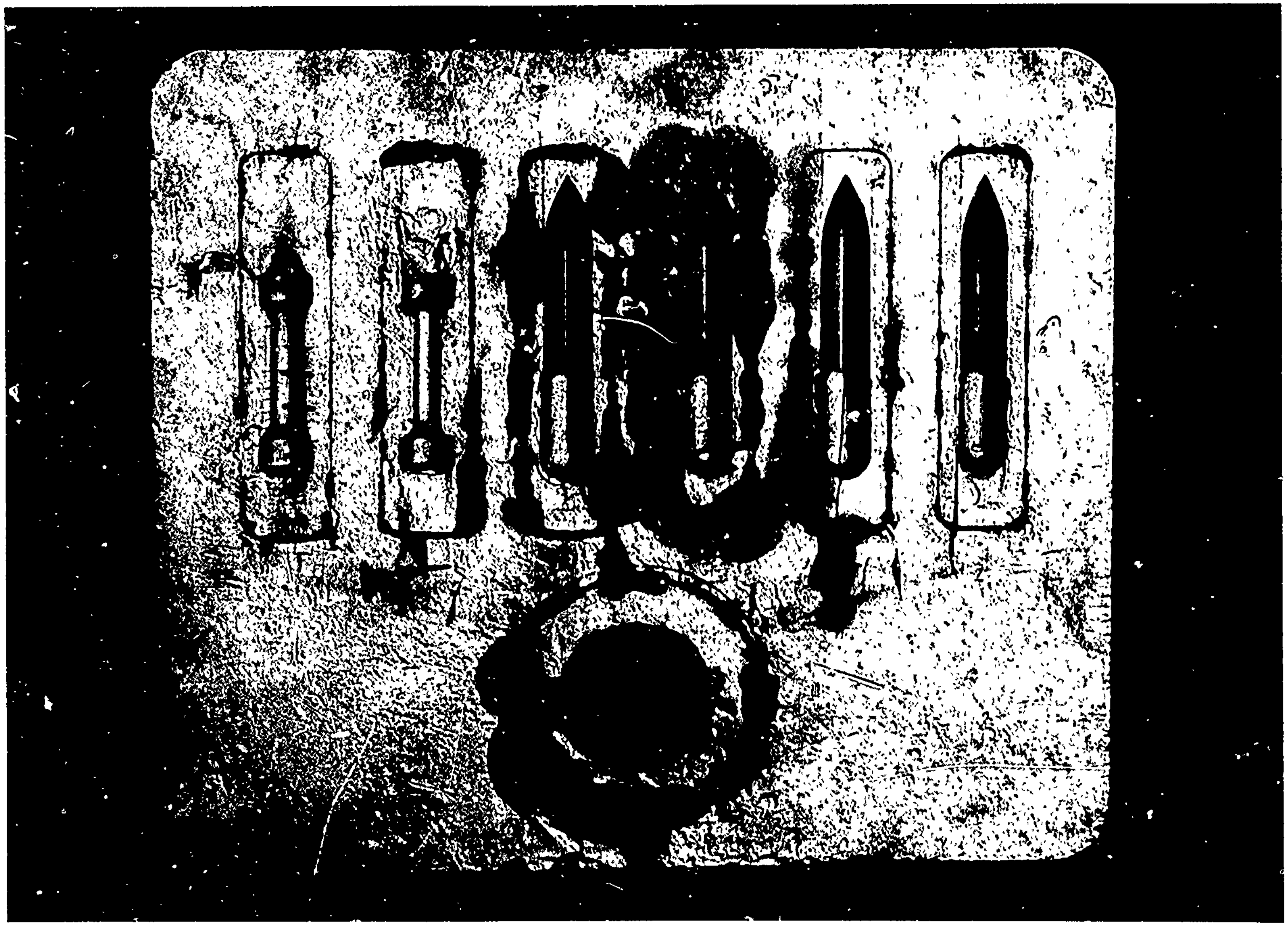


Figure 124 - Cover half of the materials evaluation die after 8992 shots. From left to right, the inserts are: Anviloy 1150--8992 shots; pressed and sintered molybdenum--8992 shots; Mo-3--8992 shots; 80-20--8992 shots; TZM-8992 shots; high-density, pressed and sintered molybdenum--8992 shots.

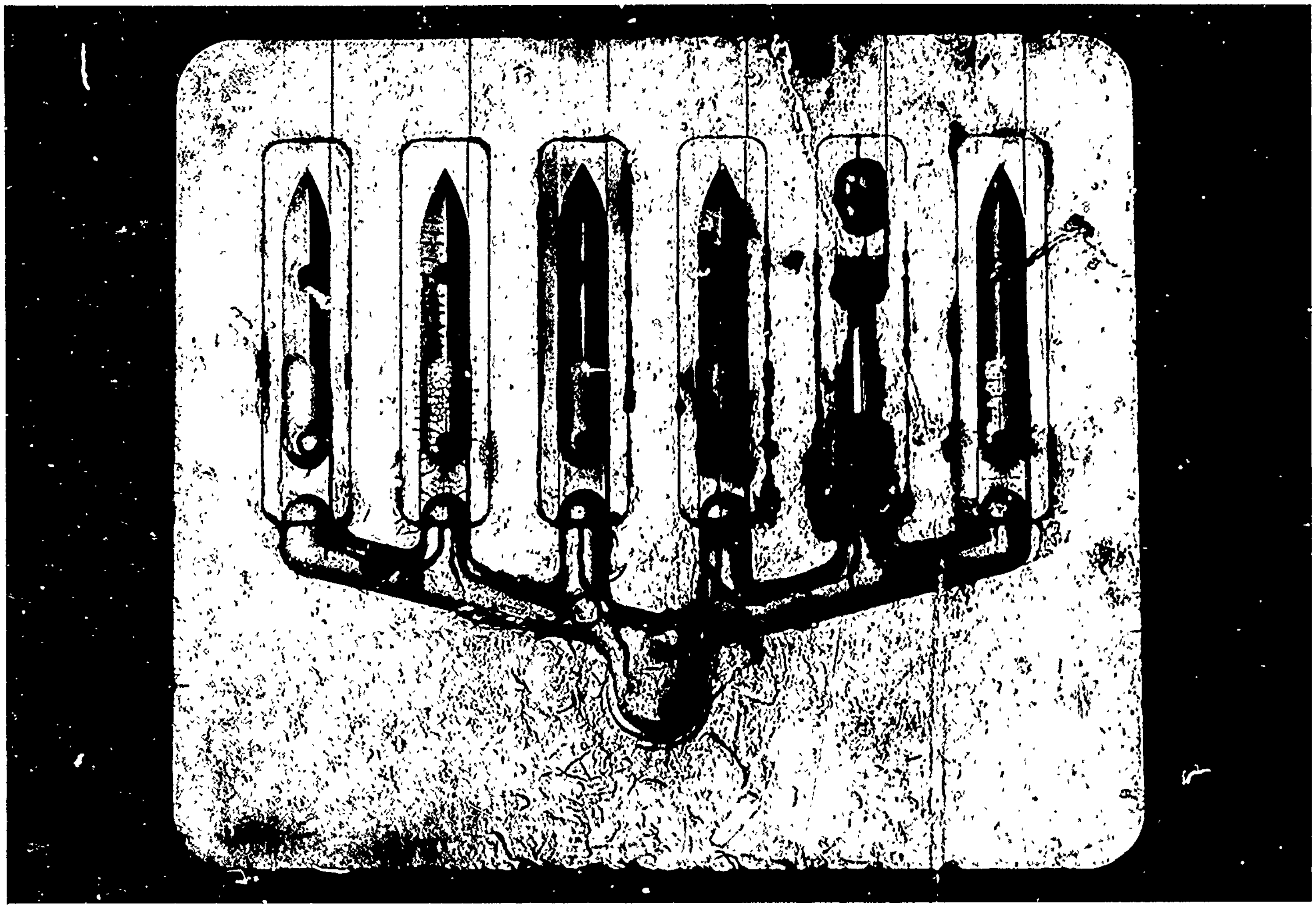


Figure 125 - Ejector half of the materials evaluation die after 8992 shots. From left to right, the materials are: high-density, pressed and sintered molybdenum--2230 shots; HOT SHOT 2920X--8737 shots; Cb-25% Zr--2230 shots; wrought molybdenum--8990 shots; wrought molybdenum--8992 shots; HOT SHOT 2920X--8264 shots.

Table XL--Measurement of the Gap Formed Between the Insert Retainer Plates and the Gate End of the Inserts After 8992 Shots (and Between the Cover Half Retainer Plate and the Shot Sleeve Bushing)

Cover Half

Position	Gap (in)	Insert Material
1	0.039	Anviloy 1150
2	0.045	Pressed and sintered Mo
3	0.054	Mo-3
4	0.056	80-20
5	0.053	TZM
6	0.035	Pressed and sintered Mo
Bushing	0.034	Wrought Mo--1550 shots

Ejector Half

Position	Gap (in)	Insert Material
1	0.042	Cu-infiltrated Mo--2745 shots Cb-752--4017 shots Pressed and sintered Mo--2230
2	0.034	Cb-25% Zr--255 shots HOT·SHOT 2920X--8737 shots
3	0.049	GE-474--7017 shots Cb-25% Zr--1975 shots
4	0.045	Cu-infiltrated W--2 shots Wrought Mo--8990 shots
5	0.038	Wrought Mo
6	0.033	H-13--728 shots HOT SHOT 2920X--8264 shots

Table XLI--Diameters of the Ejector Pin Holes in the Materials Evaluation Die After 8992 Shots

Component	Material	Condition	No. of Shots	Position	Diameter (in)	Original Diameter (in)
Insert retainer plate	High-density pressed & sintered molybdenum	Stress relieved	8992	1	0.276	0.250 -0.251
				2	Plugged	0.250 -0.251
				3	Plugged	0.250 -0.251
				4	0.260	0.250 -0.251
Insert #1 replacement	High-density pressed & sintered molybdenum	Stress relieved	2230	Lower		0.1875-0.1885
				Upper	0.188	0.1875-0.1885
Insert #2 replacement	HOT SHOT 2920X	Stress relieved 800°C	7937	Lower	0.1885	0.1875-0.1885
				Upper	0.191	0.1875-0.1885
Insert #3 replacement	Cb-25% Zr	Stress relieved	2230	Lower	0.193	0.1875-0.1885
				Upper	0.202	0.1875-0.1885
Insert #4 replacement	Wrought molybdenum	As machined	8990	Lower	0.225-0.228	0.1875-0.1885
				Upper	0.203-0.211	0.1875-0.1885
Insert #5 Original	Wrought molybdenum	Stress relieved	8992	Lower	0.196	0.1875-0.1885
				Upper	0.195	0.1875-0.1885
				Overflow	0.1875	0.1875-0.1885
Insert #6 replacement	HOT SHOT 2920X	Solution annealed and stress relieved	8264	Lower	0.188	0.1875-0.1885
				Upper	0.190	0.1875-0.1885

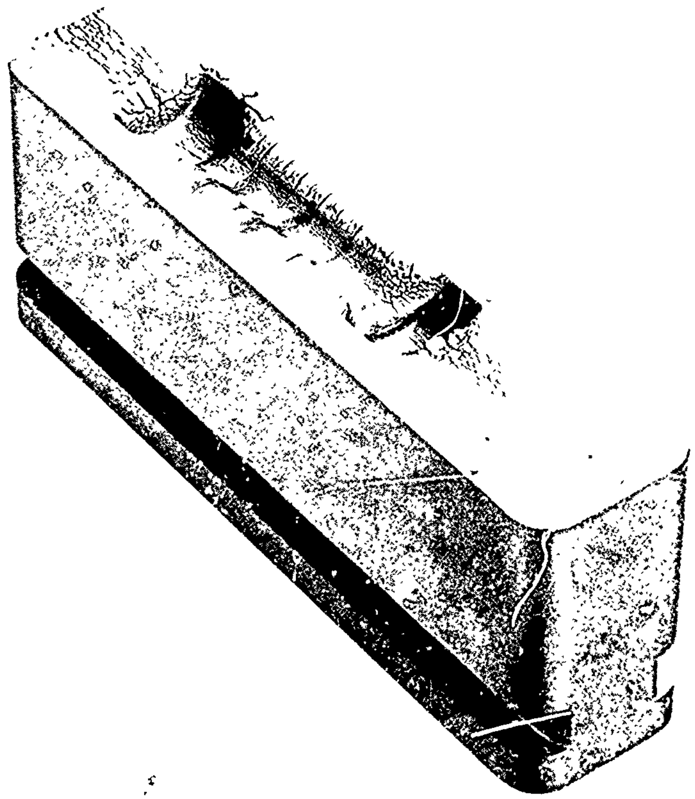


Figure 126 - Anviloy 1150 insert removed from the materials evaluation die after experiencing 8992 shots.

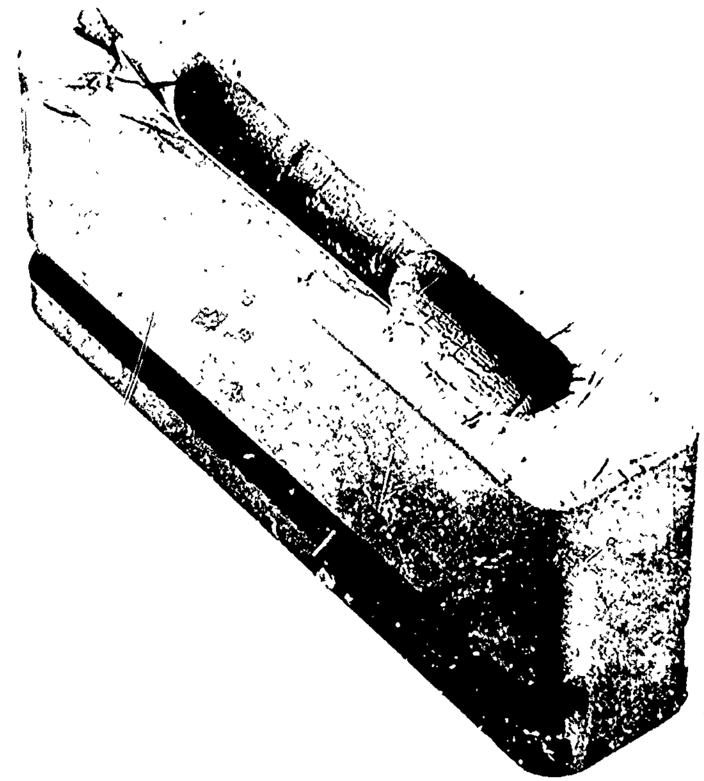


Figure 127 - 80-20 insert removed from the materials evaluation die after experiencing 8992 shots.

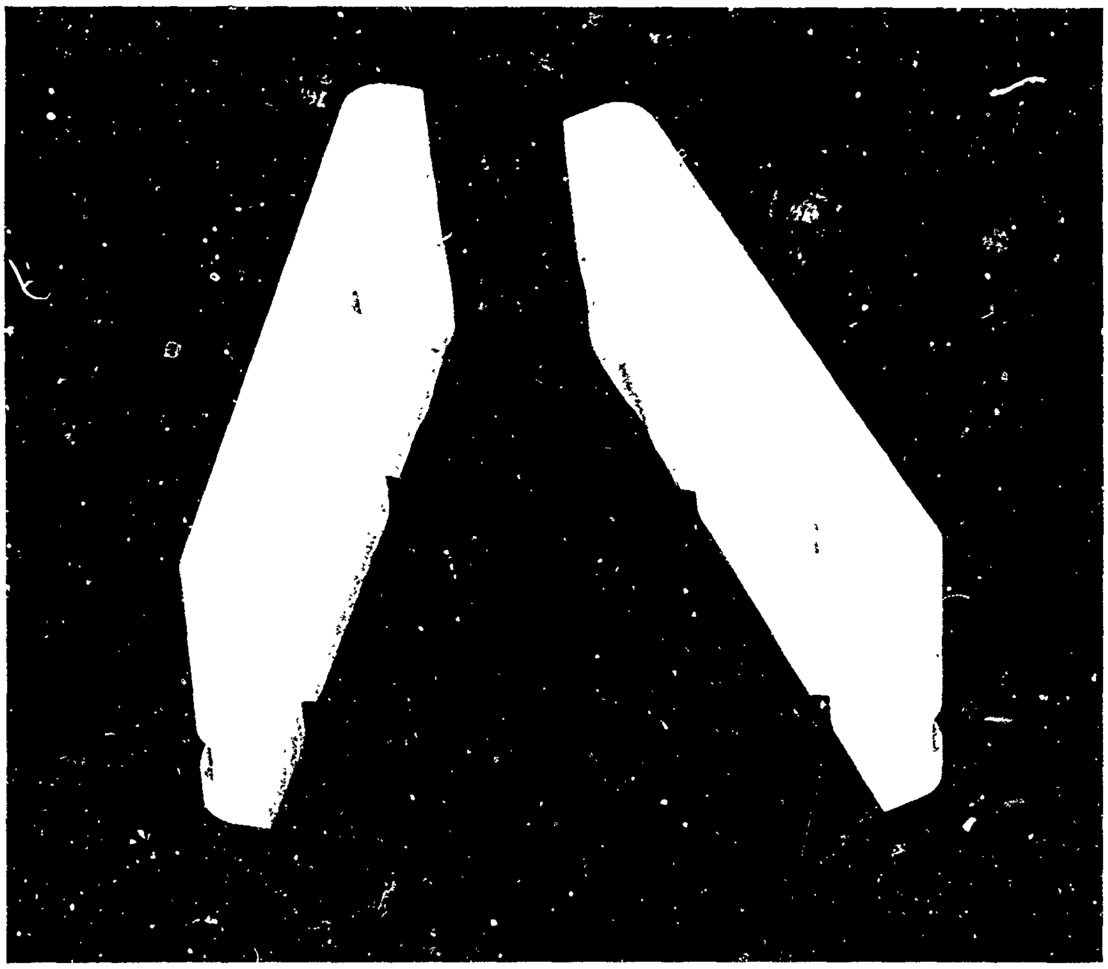


Figure 128 - As-machined, wrought molybdenum insert that split along an existing crack while being removed from the materials evaluation die after experiencing 8990 shots. Note the absence of heat checking.

The replacement insert retainer plates were fabricated by the Dover Wire and Fabrication Operation of the Lamp Metals and Components Department from wrought molybdenum which had been extruded from an 11" diameter powder metallurgy billet to a 3" by 8" rectangle and cross rolled to a 1-5/8" plate. After rolling, the plate was stress relief annealed at 925°C for one hour. In spite of that anneal, the insert retainer plates were noted to have warped after they had been rough machined. Therefore, they were reannealed at 850°C before final machining. A fluorescent die penetrant inspection of the completed plates revealed a multitude of longitudinal cracks in the pockets and along the edges of the plates. (Based on prior experience, these cracks were attributed to the thermal shock resulting from the caustic cleaning operation which was employed after the plates had been stress relief annealed. The plates were plunged directly into the 800°F molten caustic without preheating. The residual stresses imposed by the caustic cleaning may also have been responsible for the warpage observed.) Because no cracks were observed in the parting faces, sprue, or runners of the insert retainer plates, it was decided that they could be used.

During assembly, several inserts seized in the cover half retainer plate. When an attempt was made to press them into the pockets with an arbor press, a large flat piece of material shaled away from the back surface of the retainer plate, beneath Pockets 1, 2, and 3. Figure 129 is a photograph taken of the cover-half insert retainer plate after the broken piece was milled away. When an attempt was made to press the inserts out, the face of the insert retainer plate was similarly damaged, as can be seen in Figure 131. In spite of the extensive damage, however, the cover-half insert retainer plate remained functional.

The Mo-3 insert was also damaged when it seized in the retainer plate and was subsequently pressed out. The loss of that insert, which was still producing good quality castings after 8992 shots, would have been particularly frustrating, inasmuch as it had taken months to accumulate that history of service. Fortunately, it was found possible to repair the Mo-3 with an insert fabricated from high-density, pressed and sintered molybdenum and to keep the Mo-3 insert on test. Figure 130 illustrates the repair insert and the repaired Mo-3 insert.

When the die was finally reassembled, the original-style Anviloy 1150 insert in cover Impression 1 was replaced with an original-style, machinable grade, pressed and sintered tungsten insert. The replacement-style 80-20 insert in cover Impression 4 was replaced with a silicided, wrought molybdenum insert. The as-machined, replacement-style wrought molybdenum insert in ejector Impression 4 was replaced with an original-style Anviloy 1150 insert.

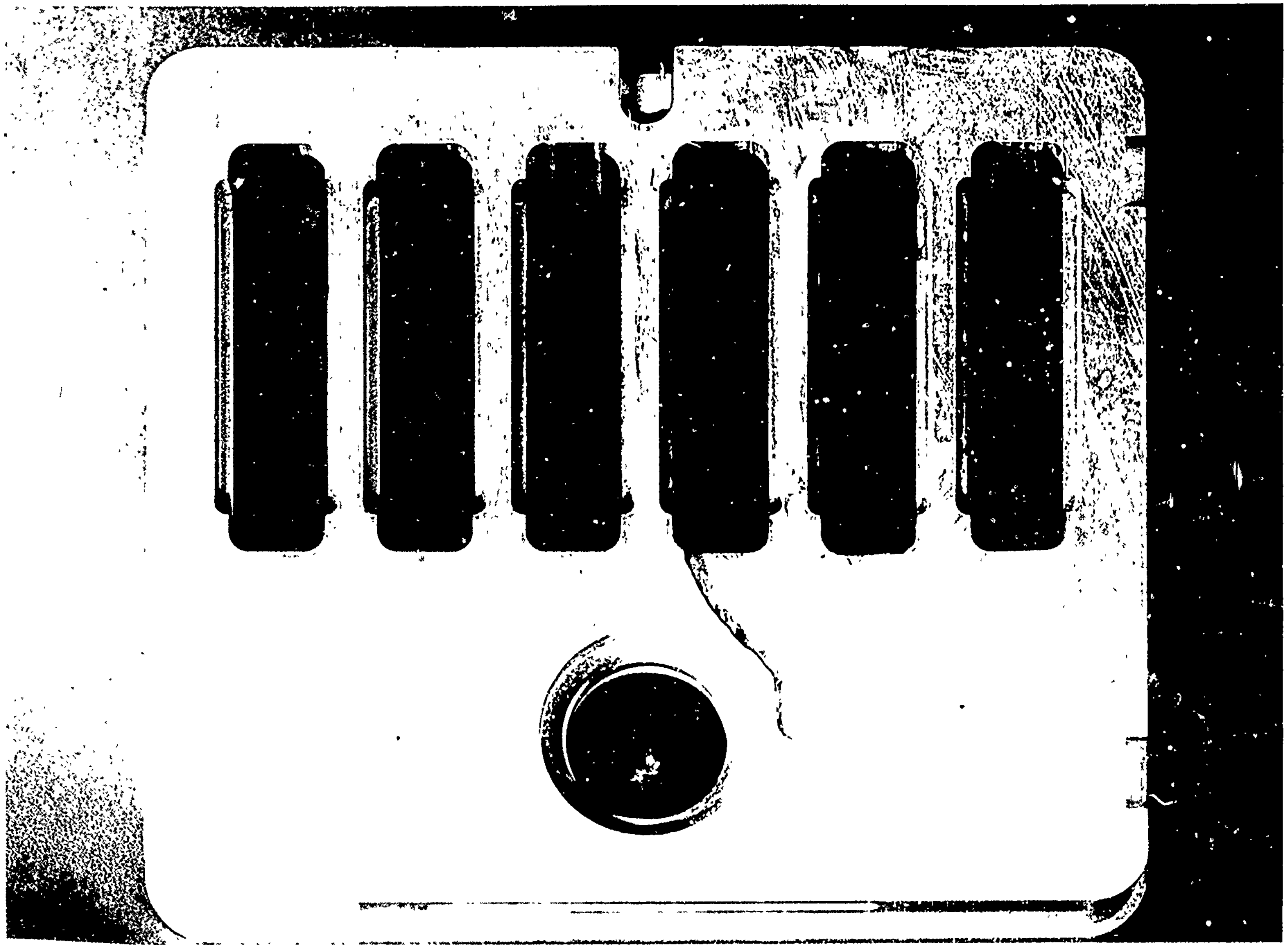


Figure 129 - Wrought molybdenum, cover-half insert retainer plate damaged during assembly of the die.

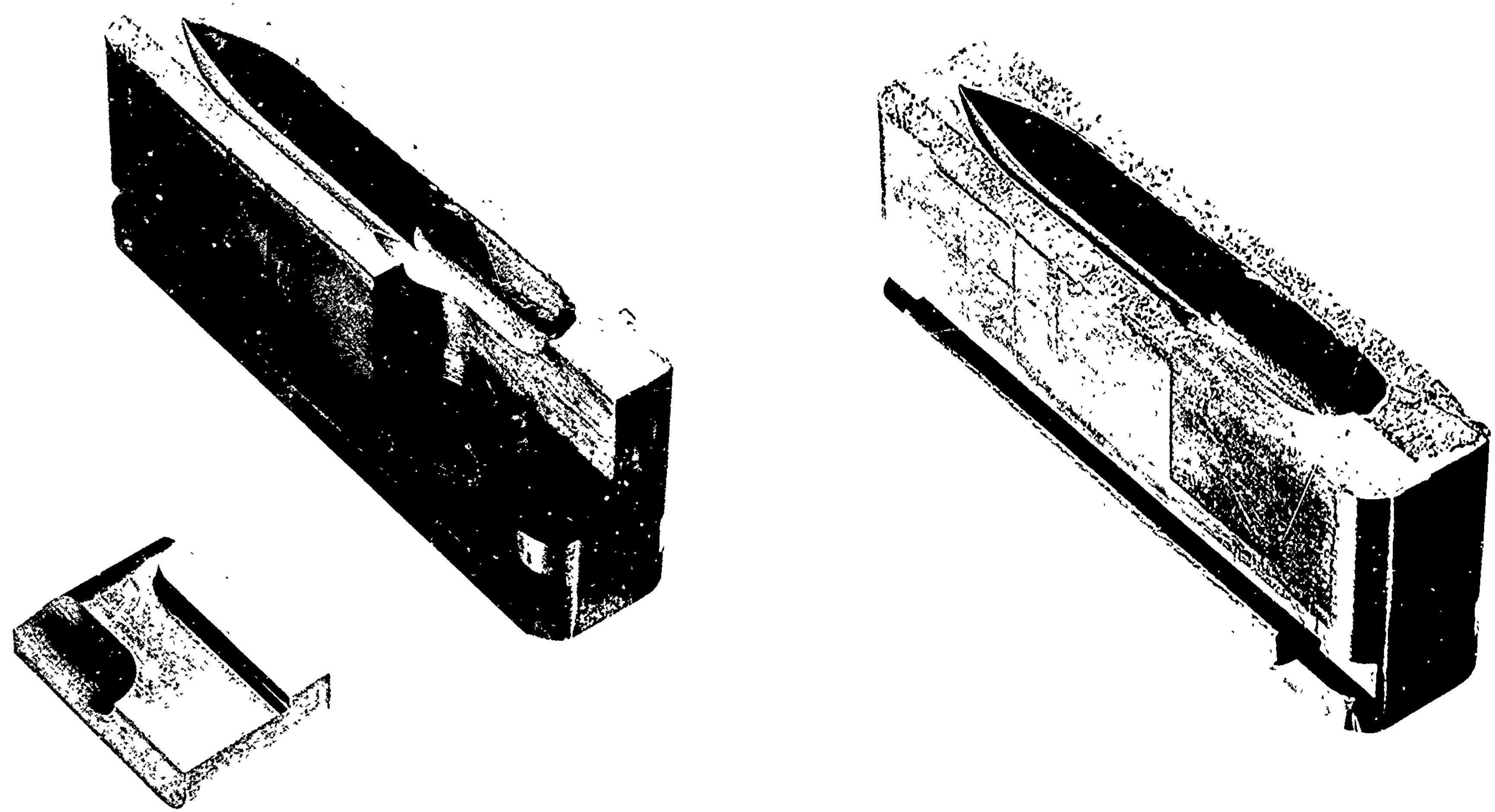


Figure 130 - High-density, pressed and sintered molybdenum insert designed to repair the damaged Mo-3 insert (left); repaired Mo-3 insert (right).

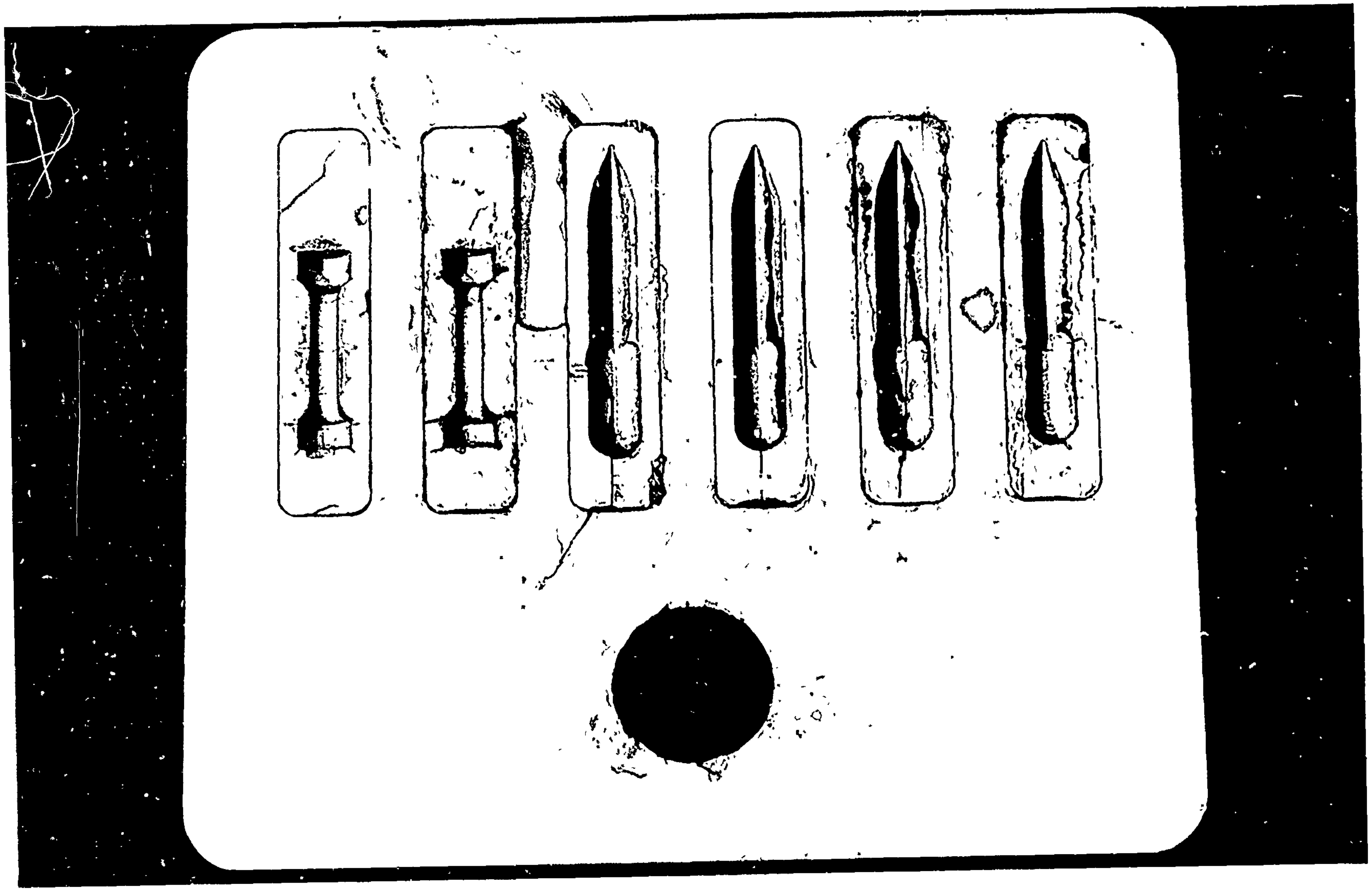


Figure 131 - Cover half of the materials evaluation die after 11,577 shots (2585 shots on the insert retainer plate). From left to right, the inserts are: pressed and sintered tungsten--2585 shots; high-density, pressed and sintered molybdenum--11,577 shots; Mo-3--11,577 shots; silicided wrought molybdenum--2585 shots; TZM--11,577 shots; high-density, pressed and sintered molybdenum--11,577 shots.

The die was reassembled with the full complement of ejector pins made from wrought molybdenum. In an effort to keep the die cool, the insulation between the top clamping plate and the stationary platen was reduced from 1" to 1/2". The plunger rod and the shot sleeve were correspondingly shortened 1/2". Testing was resumed.

The die was again removed for inspection and photographed after a cummulative total of 11,577 shots had been made. The condition of the die at that point is indicated by Figures 131 and 132. The results of the inspection are presented in detail in Tables XLII and XLIII.

As a result of that inspection, the Anviloy 1150 insert in ejector Impression 4, which had experienced only 2585 shots, was judged unfit for further service. It was replaced by an original-style, pressed and sintered tungsten-2% thoria insert. The condition of the Anviloy insert removed from test is indicated by Figure 133.

The evaluation proceeded until the cumulative total of 15,047 shots had been made. At that point, the die was removed from the machine, photographed, and inspected. Figures 134 and 135 portray the condition of the die after 15,047 shots. Table XLIV describes the results of the visual inspection of the die. In considering these data, it is important that it be understood that, unless otherwise noted, each of the inserts continued to produce acceptable to superior castings. This point is demonstrated very graphically by Figures 136 and 137.

The gaps between the inserts and the insert retainer plates, formed by exposure to high pressure molten metal, were measured and are recorded in Table XLV. The diameters of the ejector pin holes were measured and are recorded in Table XLVI. The ejector pins were also measured, and their performance is recorded in Table XLVII.

While making these measurements, a rather remarkable change was noted in the diameter of the shot sleeve hole bored in the cover-half insert retainer plate. Near the parting line, the diameter of that hole had decreased from 1.506" to 1.487"-1.489"; and approximately 1-1/4" below the parting line, the diameter had decreased from 1.506" to 1.503"-1.504". No satisfactory explanation for this phenomenon has been proposed.

Due to a lack of additional funding, the life testing of die materials under this contract was terminated, after a cumulative total of 15,047 shots had been made by the Lamp Metals and Components Department.

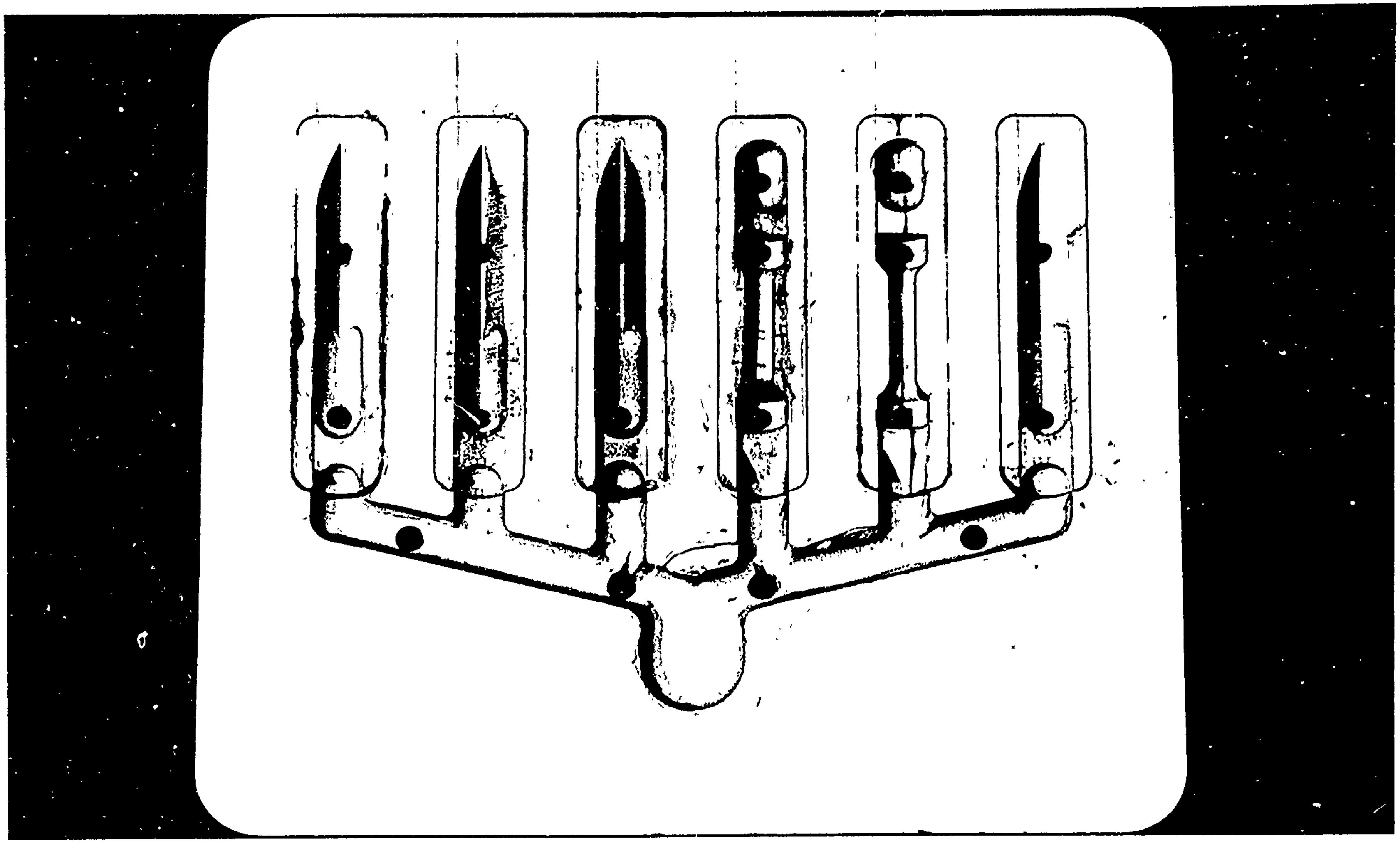


Figure 132 - Ejector half of the materials evaluation die after 11,577 shots (2585 shots on the insert retainer plate). From left to right, the inserts are: high-density, pressed and sintered molybdenum--4815 shots; HOT SHOT 2920X--11,322 shots; Cb-25% Zr--4815 shots; Anviloy 1150--2585 shots; wrought molybdenum--11,577 shots; HOT SHOT 2920X--10,849 shots.

Table XLII--Results of Inspection of the Materials Evaluation Die After 11,577 Shots

Position	Material	Condition	Insert Style	No. of Shots	Remarks
Cover half	Wrought molybdenum	Stress relieved	Retainer plate	2,585	End mill reference point approximately 1" from the shot sleeve hole at ten o'clock to the hole is connected to the lower left corner of Pocket 3 by a crack, a small chip has broken out of the edge of the shot sleeve hole at 11 o'clock, small cracks noted in edge of shot sleeve hole at ten and two o'clock opposite the runners, lips raised at the gate-end of Pockets 3, 4, and 5, surface not raised opposite runners, no heat checking.
1	Pressed and sintered tungsten	Stress relieved	Original	2,585	Area opposite gate heat checked, gate-end edge of cavity badly erroded, edge of cavity along reduced section and at vent end deteriorated, chip broken from gate-end edge of insert, three large cracks traverse the reduced section, four smaller transverse cracks extend from the edge of the reduced section into the cavity, one crack traverses the gate-end shoulder of the cavity just below the radiused section, very small circumferential cracks noted in grooves at both ends of the upper shoulder of the cavity and at the gate-end of the cavity, longitudinal crack noted in lower shoulder of cavity, the insert is in poor condition, judged marginally useful.

Table XLII--Results of Inspection of the Materials Evaluation Die After 11,577 Shots
(Continued)

Position	Material	Condition	Insert Style	No. of Shots	Remarks
2	High-density pressed & sintered molybdenum	Stress relieved	Original	11,577	Heat checking now observed in the upper shoulder and radiused section has become more obvious in areas closer to gate, circumferential crack formed by groove between lower shoulder and radiused section enlarged, now extends into parting line, small single longitudinal cracks noted in edge of insert and edge of cavity at gate end, two small longitudinal cracks noted in edge of cavity at vent end, small transverse crack noted in flat opposite gate.
3	Mo-3	Stress relieved	Replacement	11,577	Transverse segment only, of interface between Mo-3 and repair insert has opened, lip raised at gate end of insert is noticeably higher for the pressed and sintered molybdenum repair insert than for the Mo-3, two small transverse cracks noted in Mo-3 opposite gate, otherwise little changed.
4	Silicided wrought molybdenum	As silicided	Replacement	2,585	High lip raised at gate end of insert, longitudinal crack noted from gate end of insert, along the apex of the triangular section to the point at which the groove swings out toward the parting line, no heat checking observed.

Table XLII--Results of Inspection of the Materials Evaluation Die After 11,577 Shots
(Continued)

Position	Material	Condition	Insert Style	No. of Shots	Remarks
5	TZM	Stress relieved	Replacement	11,577	Half-round section has begun to heat check, longitudinal crack is opening up in the half-round section only, pitting has progressed into the triangular section.
6	High-density pressed & sintered molybdenum	Stress relieved	Replacement	11,577	Transverse cracks noted in the edge of the cavity along the half-round section and the lower end of the triangular section, a crack at the gate end of the cavity follows a machining mark left by the ball end mill around the semicircular end of the cavity.
Ejector half	Wrought molybdenum	Stress relieved	Retainer plate	2,585	Large thin pieces have broken out of the parting line (or shaled away) in the corner between the main runner and the vertical segment feeding cavity Number 1 and above the sprue between the vertical segments feeding Cavities 3 and 4, the upper edge of the main runner between Pockets 4 and 5 has broken away, small pieces have broken away (or shaled) from the lower left corner of Pocket 1, the lower right corner of Pocket 6,and the bottom of the vertical segment of the runner feeding cavity Number 2, lips have been raised around each ejector pin hole, cracks extend from the inboard ejector pin holes up the vertical segment of the runners feeding Cavities 3

Table XLII--Results of Inspection of the Materials Evaluation Die After 11,577 Shots
(Continued)

Position	Material	Condition	Insert Style	No. of Shots	Remarks
					and 4, very small, short cracks also noted where the sprue joins the runner and in, and parallel to, the runners above and to the left of the sprue and in the vertical segments leading to Cavities 3, 4, and 5, lips are raised at the gate end of Pockets 2 through 6, the parting line surface opposite the lip of the shot sleeve liner is <u>not raised</u>.
1	High-density pressed & sintered molybdenum	Stress relieved	Replacement	4,815	The lip raised around the lower ejector pin hole is cracked, <u>no</u> heat checking.
2	HOT SHOT 2520X	Stress relieved	Replacement	11,322	Virtually unchanged from the previous inspection.
3	Cb-25% Zr	Stress relieved	Replacement	4,815	Heat checking has progressed into the triangular section, condition of half-round section little changed except for some indication of erosion, runner end also appears slightly eroded, heat checking appears to have been minimized around lower ejector pin which presumably acted as a heat sink.
4	Anviloy 1150	Stress relieved	Original	2,585	Only minor heat checking, but all edges are badly eroded, judged <u>unfit</u> for further service.

Table XLII--Results of Inspection of the Materials Evaluation Die After 11,577 Shots
(Continued)

Position	Material	Condition	Insert Style	No. of Shots	Remarks
5	Wrought molybdenum	Stress relieved	Original	11,577	Existing longitudinal crack has opened in both radiused sections, circumferential cracks noted at both ends of the lower shoulder, no heat checking.
6	HOT SHOT 2920X	Solution annealed and stress relieved	Replacement	10,849	Virtually unchanged from previous inspection.

Table XLIII--Measurement of the Gap Formed Between the Insert Retainer Plates and the Gate End of the Inserts After 11,577 shots (2,585 Shots on the Retainer Plates)

Position	Gap (in)	Insert Material
	Cover Half	
1	0.014	Pressed and sintered W--2585 shots
2	0.025	Pressed and sintered Mo--11,577 shots
3	0.025	Mo-3--11,577 shots
4	0.029	Silicided, wrought Mo--2585 shots
5	0.018	TZM--11,577 shots
6	0.022	Pressed and sintered Mo--11,577 shots
	Ejector Half	
1	0.030	Pressed and sintered Mo--4815 shots
2	0.021	HOT SHOT 2920X--11,322 shots
3	0.021	Cb-25% Zr--4815 shots
4	0.016	Anviloy 1150-2585 shots
5	0.018	Wrought Mo--11,577 shots
6	0.025	HOT SHOT 2920X--10,849 shots

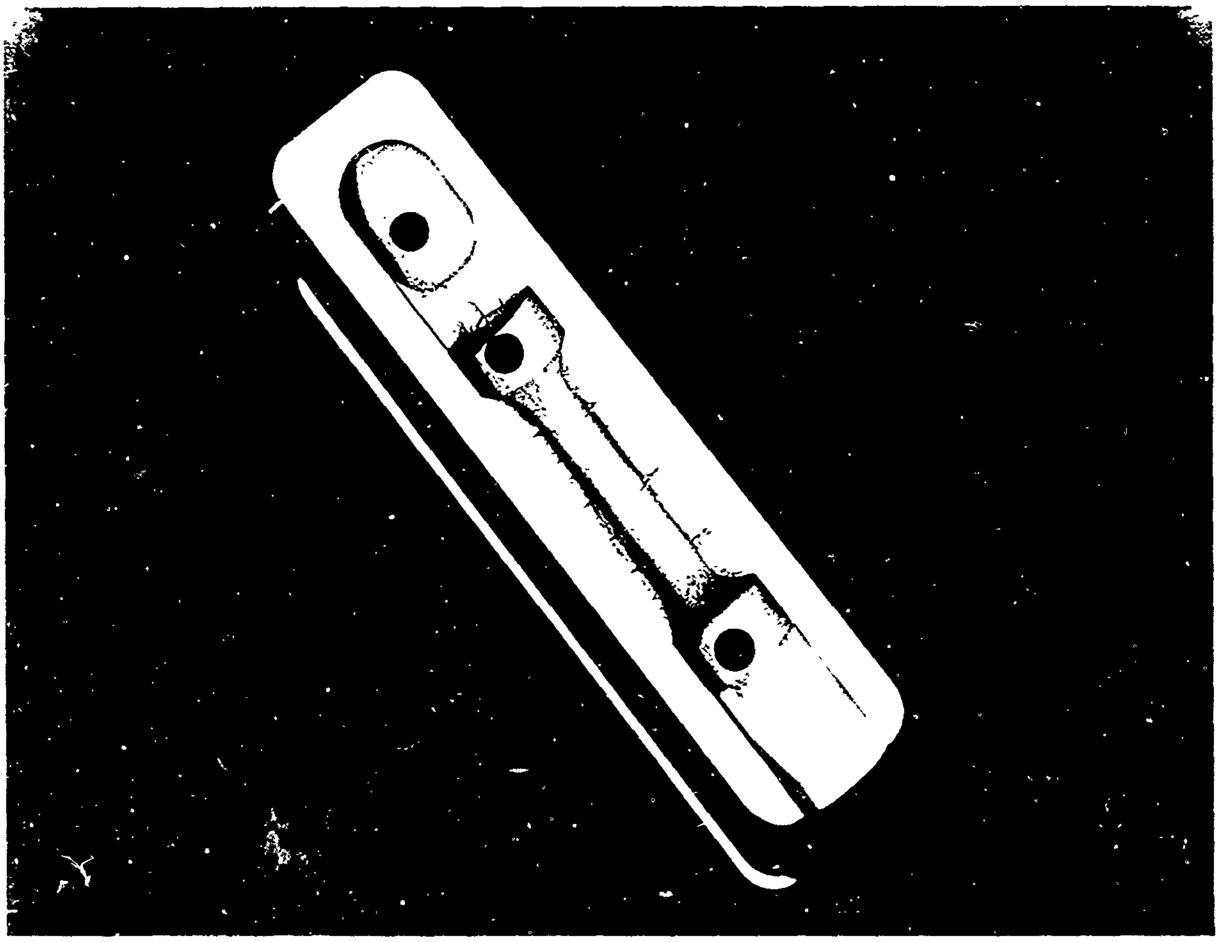


Figure 133 - Anviloy 1150 insert removed from the materials evaluation die after experiencing 2585 shots. Note the erosion.

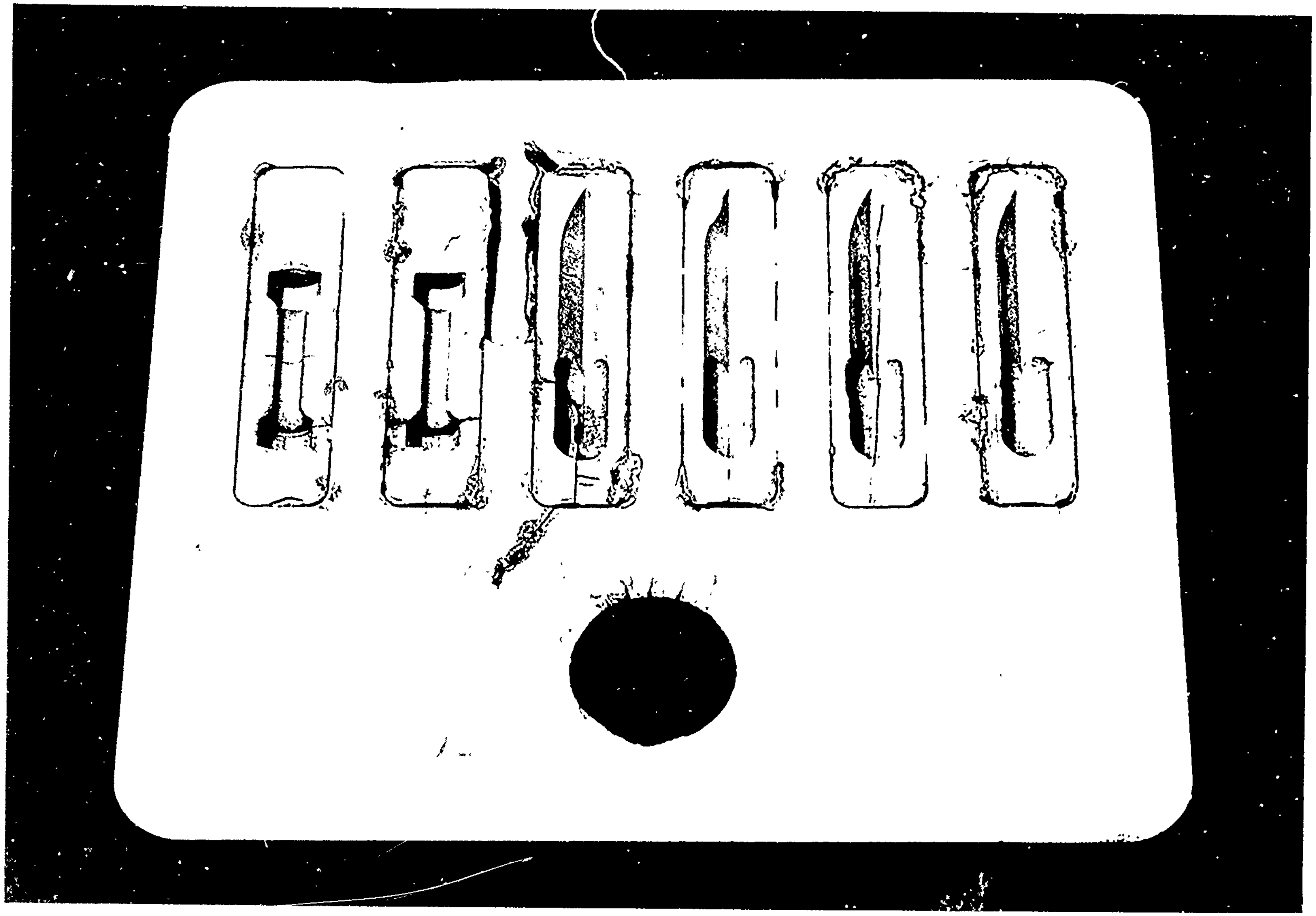


Figure 134 - Cover half of the materials evaluation die after 15,047 shots (6,055 shots on the insert retainer plate). From left to right, the inserts are: pressed and sintered tungsten--6,055 shots; high-density, pressed and sintered molybdenum--15,047 shots; Mo-3--15,047 shots; silicided, wrought molybdenum--6,055 shots; TZM--15,047 shots; high-density, pressed and sintered molybdenum--15,047 shots.

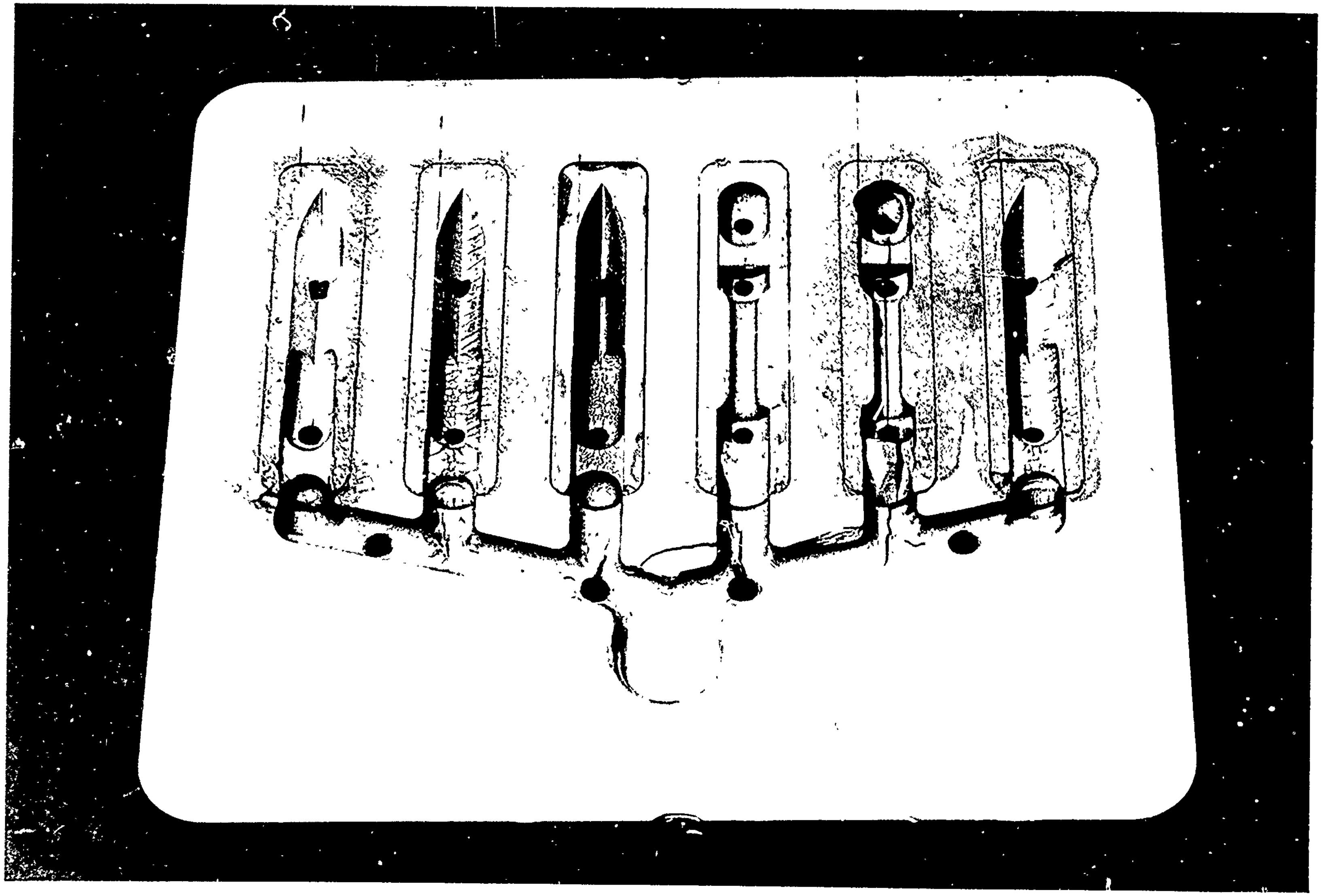


Figure 135 - Ejector half of the LMCD materials evaluation die after 15,047 shots (6,055 shots on the insert retainer plate). From left to right, the inserts are: high-density, pressed and sintered molybdenum--8,285 shots; HOT SHOT 2920X--14,792 shots; Cb-25% Zr--8,285 shots; pressed and sintered tungsten 2% thoria--3,470 shots; wrought molybdenum--15,047 shots; HOT SHOT 2920X--14,319 shots.

Table XLIV--Results of Inspection of the Materials Evaluation Die After 15,047 Shots

Position	Material	Condition	Insert Style	No. of Shots	Remarks
Cover half	Wrought Mo	Stress relieved	Retainer plate	6,055	Three prominent cracks have formed in the upper edge of the shot sleeve port; very fine, predominantly vertical heat checking has spread out in the runner system from the shot sleeve port to the right edge of Pocket 2 and to the left edge of Pocket 5; accelerated heat checking has been initiated at the end mill reference point; a light scratch across the runner system has also served as a source of crack initiation; a number of small, thin flakes have spalled from the face of the plate around the shot sleeve hole; a raised replica of runner system is just discernable
1	Pressed and sintered W	Stress relieved	Original	6,055	A chip has broken out of the parting line between the lower shoulder of the cavity and the right edge of the insert; a transverse crack in center of reduced section extends completely across insert; very fine heat checking noted in the radiused sections, in parting line along reduced section, and opposite tip and in-gate of the opposing replacement-style insert; very small longitudinal cracks noted in both shoulders of cavity; the insert is still <u>marginally useful</u>
2	High-density, pressed and sintered Mo	Stress relieved	Original	15,047	Fine heat checking noted throughout cavity becomes progressively finer at the upper end; where the retainer plate was broken away from the right edge of the pocket, the parting line of the unsupported insert is badly dented and no longer locks up tightly,

Table XLIV--Results of the Materials Evaluation Die After 15,047 Shots
(Continued)

Position	Material	Condition	Insert Style	No. of Shots	Remarks
					the transverse crack between the lower shoulder and the reduced section is quite large; quality of castings remains quite good, but failure to lock-up suggests replacement
3	Mo-3	Stress relieved	Replacement	15,047	Fine heat checking has spread throughout the cavity; very fine, irregular, longitudinal crack now extends nearly to the end of the triangular section following the apex; the left edge of the insert is dented, presumably because of lack of support from the retainer plate; quality of castings is good, little changed from previous inspection
4	Silicided, wrought Mo	As silicided	Replacement	6,055	Flat, 3/8" diameter semi-circular chip broken from parting line on left edge of cavity tip; otherwise, little changed from previous inspection
5	TZM	Stress relieved	Replacement	15,047	Very fine heat checking has spread throughout cavity; otherwise, little changed from previous inspection
6	High-density, pressed and sintered Mo	Stress relieved	Replacement	15,047	Fine heat checking has spread throughout the cavity; fine transverse cracks now extend from the half-round section of the cavity into the parting line; quality of castings remains very good
Ejector half	Wrought Mo	Stress relieved	Retainer plate	6,055	A number of thin flakes, 1/8"-3/8" diameter have spalled from the face around the sprue; very fine pitting noted in runner between

Table XLIV--Results of Inspection of the Materials Evaluation Die After 15,047 Shots

(Continued)

Position	Material	Condition	Insert Style	No. of Shots	Remarks
					Pockets 2 and 3; lips raised where runners enter Pockets 2, 3, 4, 5, and 6, and around each ejector pin hole; pronounced longitudinal cracks have developed in vertical segments of runners leading to Pockets 2, 3, 4, and 5; tracing of fine cracks noted between inboard ejector pin holes and from those holes around the bottom of the sprue, parallel to the edge
1	High-density, pressed and sintered Mo	Stress relieved	Replacement	8,285	Cracks noted to follow machining marks left by ball end mill in half-round section; lip raised around lower ejector pin hole is finely cracked; upper ejector pin hole appears to have been deformed downward; very small cracks also noted in the edge of the cavity at the in-gate and along the half-round section, right edge of in-gate appears peened over; <u>no heat checking</u> observed
2	HOT SHOT 2920X	Stress relieved	Replacement	14,792	The heat checking throughout the cavity is now becoming noticeable on the casting; some pitting or erosion noted in cavity, too; cavity continues to produce fair castings
3	Cb-25% Zr	Stress relieved	Replacement	8,285	Heat checking noted throughout cavity with notable exception of: a. Area close to the parting line b. Apex of triangular section c. Sharp corners at either edge of the in-gate; quality of castings remains good

Table XLIV--Results of Inspection of the Materials Evaluation Die After 15,047 Shots
(Continued)

Position	Material	Condition	Insert Style	No. of Shots	Remarks
4	Pressed and sintered W-2% thoria	Stress relieved	Original	3,470	A conchoidal chip with a base width equal to the in-gate has broken out of the in-gate; the edges of the fracture, the gate, and the vent are noticeably eroded, the edges of the reduced section of the cavity are eroded to a lesser degree; very fine heat checking noted on the parting face along the reduced section, but none in the cavity; a number of fine transverse cracks, extending into the parting face traverse or emanate from the cavity; casting surfaces are good, flash is excessive
5	Wrought Mo	Stress relieved	Original	15,047	Small transverse cracks noted at upper and lower end of lower shoulder; longitudinal crack extends from end-to-end of insert; edges of overflow dented; lip raised at gate-end of cavity; small longitudinal cracks along either side of the gate; thin, semi-circular piece has spalled or delaminated from left side of lower shoulder perpendicular to the parting face; <u>no heat checking observed</u>
6	HOT SHOT 2920X	Solution annealed and stress relieved	Replacement	14,319	Virtually unchanged from previous inspection; quality of castings remains good

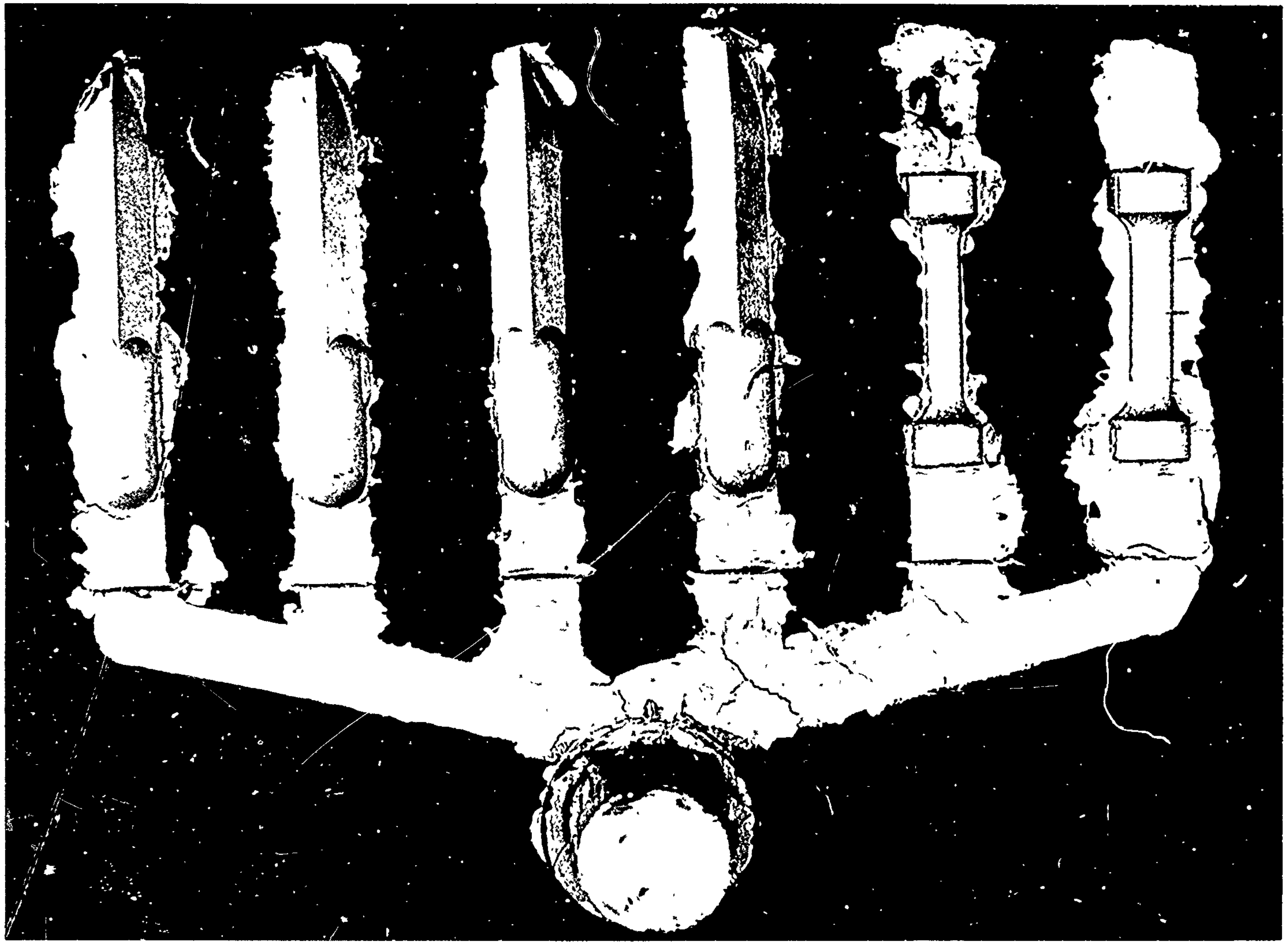


Figure 136 - Cover face view of an AISI 304 stainless steel gate cast in the materials evaluation die after 15,047 shots. From left to right, the inserts in which the castings were made are: high-density, pressed and sintered molybdenum--15,047 shots; TZM--15,047 shots; silicided wrought molybdenum--6055 shots; Mo-3--15,047 shots; high-density, pressed and sintered molybdenum--15,047 shots; pressed and sintered tungsten--6055 shots.

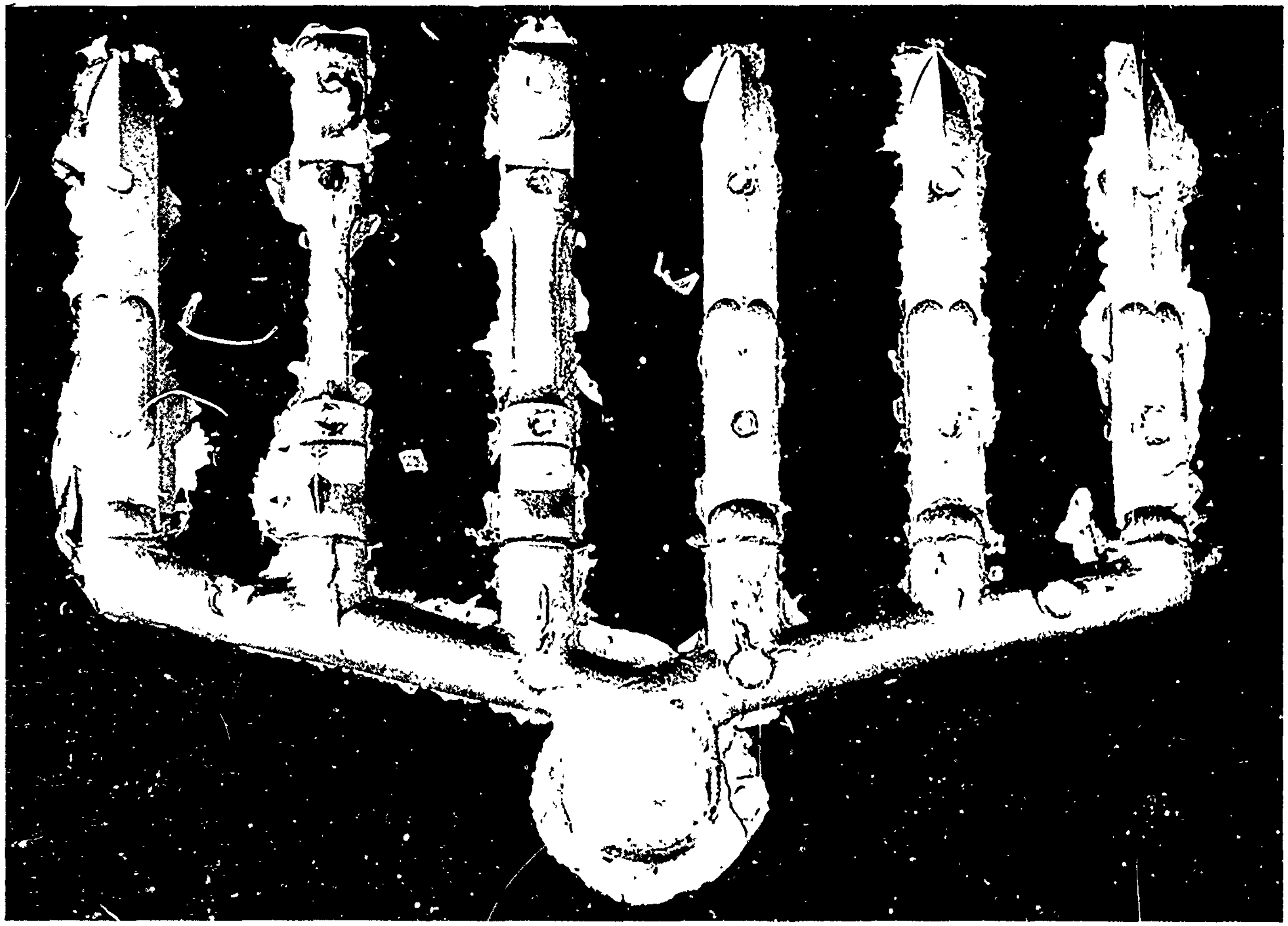


Figure 137 - Ejector face view of an AISI 304 stainless steel gate cast in the materials evaluation die after 15,047 shots. From left to right, the inserts in which the castings were made are: HOT SHOT 2920X--14,319 shots; wrought molybdenum--15,047 shots; pressed and sintered tungsten-2% thoria--3470 shots; Cb-25% Zr--8285 shots; HOT SHOT 2920X--14,792 shots; high-density, pressed and sintered molybdenum--8285 shots.

Table XLV --Measurement of the Gap Formed Between the Insert Retainer Plate and the Gate End of the Inserts After 15,047 Shots (6,055 Shots on the Retainer Plates)

Cover Half

Position	Gap (in)	Insert Material
1	0.024	Pressed and sintered W--6055 shots
2	0.031	Pressed and sintered Mo--15,047 shots
3	0.043	Mo-3--15,047 shots
4	0.047	Silicided, wrought Mo--6055 shots
5	0.024	TZM--15,047 shots
6	0.033	Pressed and sintered Mo--15,047 shots

Ejector Half

Position	Gap (in)	Insert Material
1	0.022	Pressed and sintered Mo--8285 shots
2	0.022	HOT SHOT 2920X--14,792 shots
3	0.022	Cb-25% Zr--8285 shots
4	0.024	Anviloy 1150-8585 shots Pressed and sintered W-2% ThO_2--3470 shots
5	0.024	Wrought Mo--15,047 shots
6	0.024	HOT SHOT 2920X--14,319 shots

Table XLVI--Diameters of Ejector Pin Holes in the Materials Evaluation Die After 15,047 Shots

Component	Material	Condition	No. of Shots	Position	Diameter (in)	Original Diameter (in)
Insert retainer plate	Wrought molybdenum	Stress relieved	6,055	1	0.250	0.2505-0.2515
				2	0.251-0.254	0.2505-0.2515
				3	0.238 with stainless cast in the hole	0.2505-0.2515
				4	0.253-0.262	0.2505-0.2515
Insert #1 replacement	High-density, pressed and sintered molybdenum	Stress relieved	8,285	Lower	0.221	Bored out, not measured
				Upper	0.190	0.1875-0.1885
Insert #2 replacement	HOT SHOT 2920X	Stress relieved	14,792	Lower	0.189	0.1875-0.1885
				Upper	0.189	0.1875-0.1885
Insert #3 replacement	Cb-25% Zr	Stress relieved	8,285	Lower	0.192-0.194	0.1875-0.1885
				Upper	0.192-0.193	0.1875-0.1885
Insert #4 original	Pressed and sintered W-2% ThO_2	Stress relieved	3,470	Lower	0.187	0.1875-0.1885
				Upper	0.187	0.1875-0.1885
				Overflow	0.188	0.1875-0.1885
Insert #5 original	Wrought molybdenum	Stress relieved	15,047	Lower	0.200	0.1875-0.1885
				Upper	0.200	0.1875-0.1885
				Overflow	0.196	0.1875-0.1885
Insert #6 replacement	HOT SHOT 2920X	Vacuum annealed & stress relieved	14,319	Lower	0.189	0.1875-0.1885
				Upper	0.191-0.196	0.1875-0.1885

Table XLVII--Condition of Wrought, Stress Relieved Ejector Pins After a Cumulative Total of 15,047 Shots on the Materials Evaluation Die

Die Component	Material, Condition	Pin Position	Pin Use (No. of Shots)	Diameter (in)	Condition of Pin
		0.249"-0.250" Nominal Diameter			
Insert retainer plate	Wrought molybdenum, stress relieved	1	6055	0.220	Good
		2	6055	0.247	Galled, to be replaced
		3	6055	--	Broken on removal, galled, to be replaced
		4	6055	0.231-0.248	Badly galled, slightly bent, to be replaced
		0.186"-0.187" Nominal Diameter			
Insert #1 replacement	Pressed & sintered Mo, stress relieved	Lower	6055	0.212	Oversize pin, scratched
		Upper	>6055	0.185	Good
Insert #2 replacement	HOT SHOT 2920X, stress relieved	Lower	>6055	0.184	Bent
		Upper	>6055	0.184	Bent & scratched
Insert #3 replacement	Cb-25% Zr, stress relieved	Lower	6055	0.173	Bent, to be replaced
		Upper	6055	0.187	Bent & galled, to be replaced
Insert #4 original	Pressed & sintered W-2% ThO_2, stress relieved	Lower	6055	0.180	Bent
		Upper	6055	0.183	Bent
		Overflow	6055	0.186	Good
Insert #5 original	Wrought Mo, stress relieved	Lower	6055	0.185	Good
		Upper	>6055	0.184	Good
		Overflow	6055	0.187	Bent
Insert #6 replacement	HOT SHOT 2920X, vacuum annealed & stress relieved	Lower	>6055	0.187	Good
		Upper	>6057	0.184	Bent

At that point, two inserts were removed from the die to investigate repair techniques. They were the high-density, pressed and sintered molybdenum insert from cover Impression 2, which had experienced 15,047 shots, and the HOT SHOT 2920X insert from ejector Impression 2, which had experienced 14,792 shots. As noted in Table XLIV, both continued to produce satisfactory castings, but the parting face of the molybdenum insert was badly battered, interfering with lock-up and resulting in excessive flash, and the HOT SHOT 2920X was heat checked to the point that there was some deterioration in the surface finish of the castings which it produced. The condition of these inserts can be seen in Figures 138 and 139.

An attempt has been made to summarize all of the data generated during the die materials evaluation in Table XLVIII; it represents a condensation of the information contained in Tables XXXII through XLVII and Figures 114 through 139, and it also represents a conscientious effort to rank the die materials evaluated. This was a very difficult task for a number of reasons. Not all of the materials were tested to failure; the mode of deterioration and failure was not the same for all of the components; and the performance of the components was related to their configuration, their location in the die, and the care with which they were handled. General appearance, a very subjective criterion at best, was the only standard available for ranking the materials. Nevertheless, Table XLVIII may be considered indicative of the relative merit of the materials evaluated as dies for ferrous die casting by the Lamp Metals and Components Department. The materials are listed in order of an increasing aptitude for that application. When analyzing these data, it must be borne in mind that they are very specific. Variations in the methods of manufacture of the die materials may be expected to have a profound influence on the performance of those materials. Die and pouring temperatures may likewise be expected to produce marked changes in the performance of many, if not all, of the die materials.

Valuable insights concerning the performance of dies for ferrous die casting can be gained by reconsidering each of the several modes of deterioration and failure independently.

Heat checking was eventually observed in every material evaluated, but its influence on die life was not nearly so great as it had been expected to be. It was the primary mode of deterioration only in HOT SHOT 2920X, Cb-25% Zr, Anviloy 1150, and copper-infiltrated molybdenum. In the HOT SHOT 2920X and in the Cb-25% Zr, the heat checking proceeded at a moderate pace, unaccompanied by complications. In the Anviloy 1150 and the copper-infiltrated molybdenum, however, it proceeded more swiftly and was accompanied by devastating erosion. In some of the less ductile pressed and sintered materials, such as tungsten-2% thoria, machinable tungsten, and 80-20, brittle failure may have been initiated by heat checking, the earliest checks propagating into

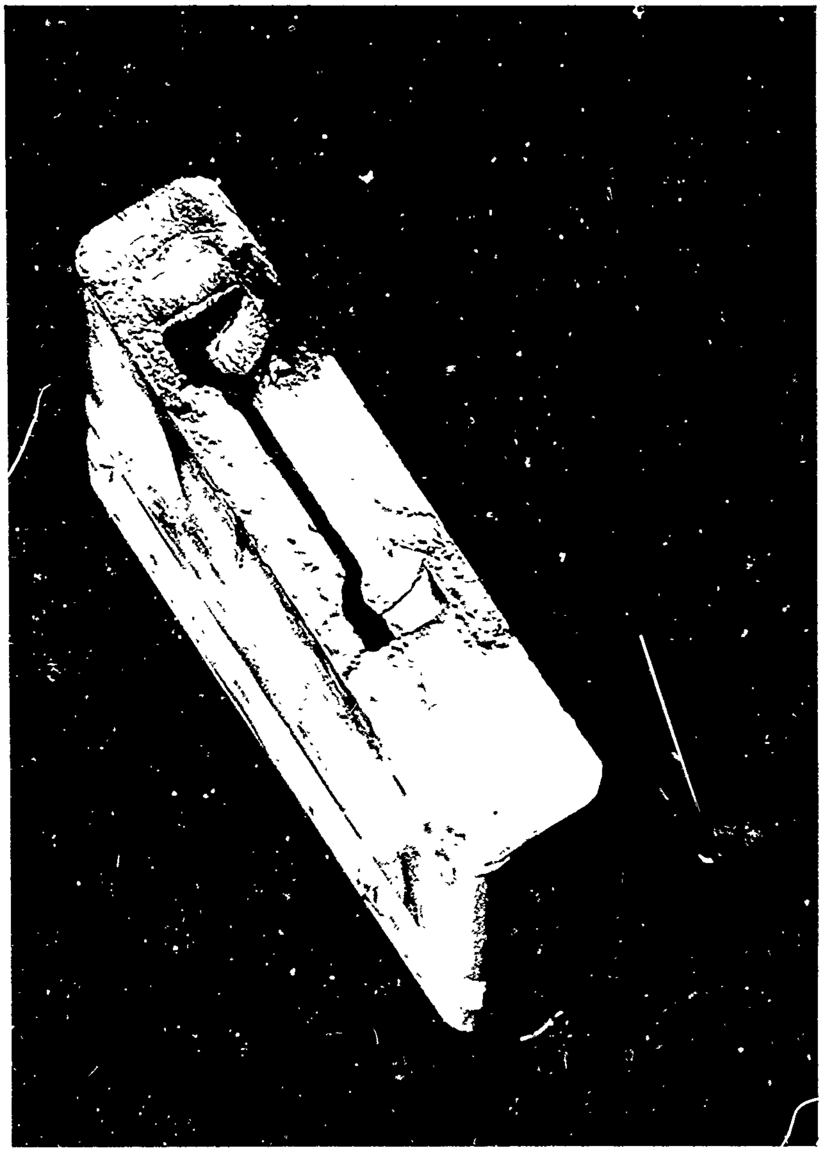


Figure 138 - High-density, pressed and sintered molybdenum insert removed from the materials evaluation die after experiencing 15,047 shots.

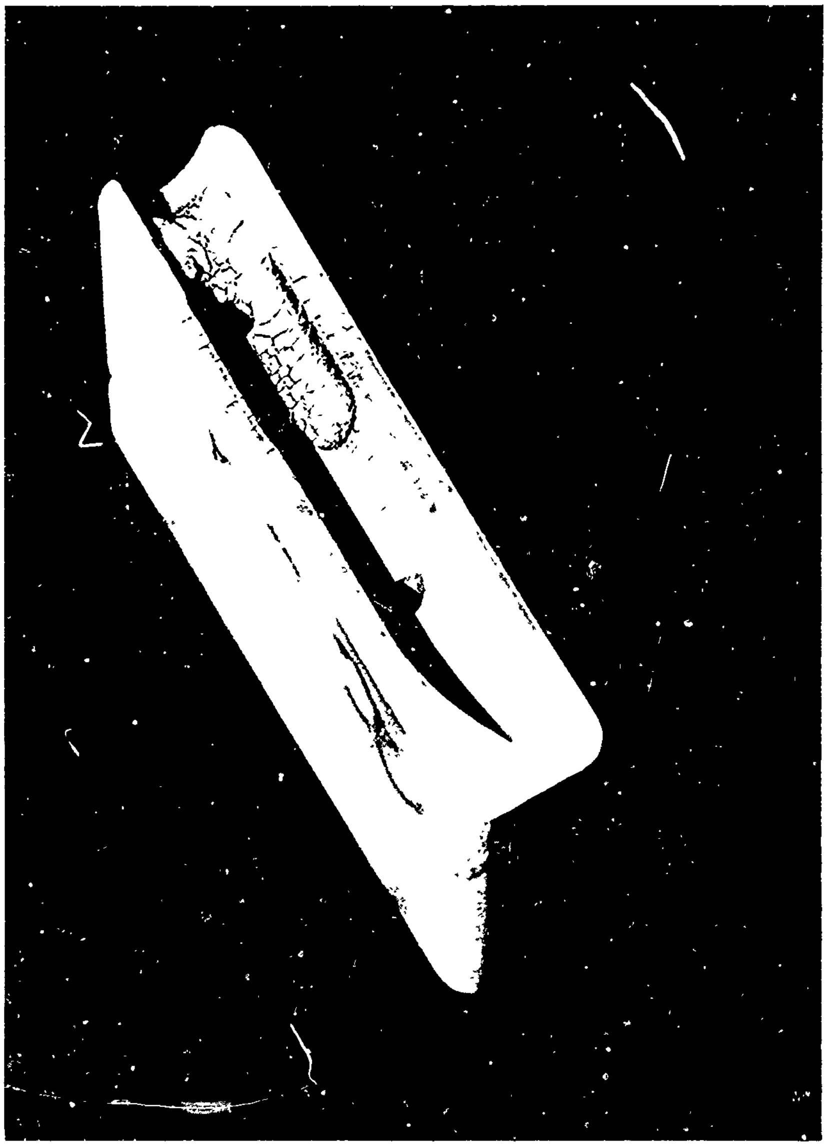


Figure 139 - HOT SHOT 2920X insert removed from the materials evaluation die after experiencing 14,792 shots.

Table XLVIII--Outcome of the Die Materials Evaluation Conducted by the Lamp Metals and Components Department

Material	Number of Cycles Experienced	Condition	Mode of Deterioration
Cu-infiltrated W	2	Failed	Brittle failure (mechanical--not in cavity)
H-13	728	Failed	Plastic deformation (cavity)
Cu-infiltrated Mo (2% Be-Cu)*	2,745	Failed	Heat checking/erosion
Anviloy 1150	2,585	Failed	Heat checking/erosion
Cb-752	4,017	Failed	Gross cracking
GE-474*	7,017	Failed	Erosion/heat checking
P&S W-2% ThO_2+	3,470	Poor	Random brittle failure/erosion
Anviloy 1150	8,992	Failed	Heat checking/erosion
P&S W+	6,055	Fair/Poor	Random brittle failure/erosion
80-20*	8,992	Failed	Random brittle failure/heat checking
Wrought Mo	8,990	Failed	Brittle failure (delamination)
Cb-25% Zr+	8,285	Fair	Heat checking
Hi-density, P&S Mo (insert retainer plates)	8,992	Failed	Gross cracking/plastic deformation
Wrought Mo (insert retainer plates)+	6,055	Good/Fair	Gross cracking/brittle failure (delamination)
Silicided wrought Mo+	6,055	Very good	Plastic deformation (gap formation) brittle failure (delamination)
Wrought Mo+	15,047	Fair/Poor	Brittle failure (delamination)
Hi-density, P&S Mo+	8,285	Very good	Plastic deformation
Hi-density, P&S Mo	15,047	Fair/Poor	Plastic deformation (gaps and dents)/gross cracking
HOT SHOT 2920X*+	14,792	Fair	Heat checking
HOT SHOT 2920X*+ (solution annealed)	14,319	Good	Heat checking
Hi-density, P&S Mo+	15,047	Good	Plastic deformation (dents)
TZM+	15,047	Good	Brittle failure (delamination)
Mo-3*+	15,047	Good	Pitting/heat checking

*Experimental materials

+On test when the evaluation was terminated

much larger cracks. In GE-474, the heat checking provided the primary site for chemical erosion. Outstanding resistance to heat checking was provided by pure wrought molybdenum, inserts of which suffered no heat checking in 15,047 cycles. Heat checking had begun in the wrought molybdenum insert retainer plates after 6055 shots, however. The effect of location was obvious, too, in other areas. The Cb-25% Zr insert resisted heat checking at the edges of the in-gate and at the apex of the triangular section, areas of low velocity. (See Figure 135.) Heat checking in the runners of the original pressed and sintered molybdenum insert retainer plate spread from the sprue outward toward the extremities of the runner system with the passage of time. (See Figures 117, 118, 120, 121, 124, and 125.) In individual inserts which displayed some resistance to heat checking, e.g., TZM, checking was noted with the passage of time to progress from an area of the cavity near the in-gate toward the vent end of the cavity.

Gross cracking was observed in Cb-752, high-density, pressed and sintered molybdenum, and wrought molybdenum. (See Figures 123, 124, 125, and 135.) These cracks were observed to have issued from ejector pin holes and the edges of the pockets in the insert retainer plates, both regions which displayed near-zero external radii. Gross cracks were also initiated at the gate ends of the tensile specimen cavities and at the intersection between those portions of the cavity which formed the shoulders and radiused sections of the die cast tensile bars. These were both regious of small internal radii. Ironically, cracks were not observed to form in the vee-grooves milled into the replacement-style inserts. Those grooves had been designed to determine the notch sensitivity of the several die materials. Because gross cracks were observed in ductile materials, and because they preceded, and in some cases, preempted, the occurrence of heat checking, it is tempting to ascribe them to a notch sensitivity much greater than that of those materials in which heat checking was the major source of failure. No efforc was made to confirm or refute that hypothesis, however.

Brittle failure was observed in copper-infiltrated tungsten; pressed and sintered tungsten-2% thoria; pressed and sintered, machinable tungsten; 80-20; wrought molybdenum; high-density, pressed and sintered molybdenum; and TZM. Brittle failures can occur in molybdenum and tungsten-base die materials, because many of them exhibit a ductile-to-brittle transition at temperatures near or above room temperature. Such materials are ductile only at temperatures above the ductile-to-brittle transition; below that temperature, they are "glass-brittle." The definition of the ductile-to-brittle transition temperature is not precise, but for the purpose of discussion, it may be arbitrarily established as that temperature at which a material will demonstrate a tensile elongation of 5%. That temperature is a function not only of the composition of the material, but also of the strain rate, the size and morphology of the grains, and the condition of stress (i.e., the dislocation density), as affected by prior plastic deformation.

The tensile properties of high-density, pressed and sintered molybdenum are plotted as a function of temperature in Figure 140. Using the arbitrary standard, the ductile-to-brittle transition appears to occur approximately at room temperature. At the die operating temperature, i.e., 400°F to 650°F, that material has a tensile elongation of approximately 40%. These data confirm the conclusion, supported by other evidence, that the brittle failure observed in the high-density, pressed and sintered molybdenum cover-half insert retainer plate occurred at room temperature and resulted from carelessly disassembling the die after 3000 shots. (See Figure 117; note that the crack is very tight; no gap has formed.)

Figure 141 compares the yield strength and ductility of pressed and sintered molybdenum with wrought (cold-worked) molybdenum in the direction of plastic flow. Several significant points should be noted:

a. The yield strength has been markedly increased

b. The ductile-to-brittle transition temperature has been depressed

c. The ductility of the wrought product is less than that of the pressed and sintered product at all temperatures above the ductile-to-brittle transition of the pressed and sintered product.

In the directions perpendicular to the direction of plastic flow, however, the situation may be quite different. For example, insert impression Block 3 in Doehler Jarvis' hemisphere die was fabricated from a rectangular bar with a 3" by 8" cross section which had been extruded from an 11" diameter pressed and sintered molybdenum billet. Tensile samples were cut from the material with their axes parallel to the 3" dimension of the extrusion (the short transverse direction) and tested at two different strain rates. The data are summarized in Table IL. They indicate that, although high strength is attained in the short transverse direction as a result of cold working, the ductile-to-brittle transition temperature is elevated to a level somewhere between 250°F and 400°F. The higher strain rate probably more nearly typifies the thermally-induced strain rate experienced by the dies. It elevates the ductile-to-brittle transition temperature of the extruded molybdenum in the short transverse direction even further, to a level somewhere between 500°F and 600°F, although it also increases the load-bearing capacity of the material somewhat.

It is the elevation of the ductile-to-brittle transition temperature in one direction that permits wrought refractory metal die components to delaminate in service. The effect may be seen in Figure 84, on the material on which the data in Table IL were generated. The same effect may be seen in the insert in Figure 85, that was fabricated from rolled molybdenum bar, and in the TZM insert in Figure 134, that was fabricated from plate. Mo-3 was the only wrought molybdenum alloy that did not suffer delamination. Because it had a recrystallized structure, its tensile properties were nearly isotropic.

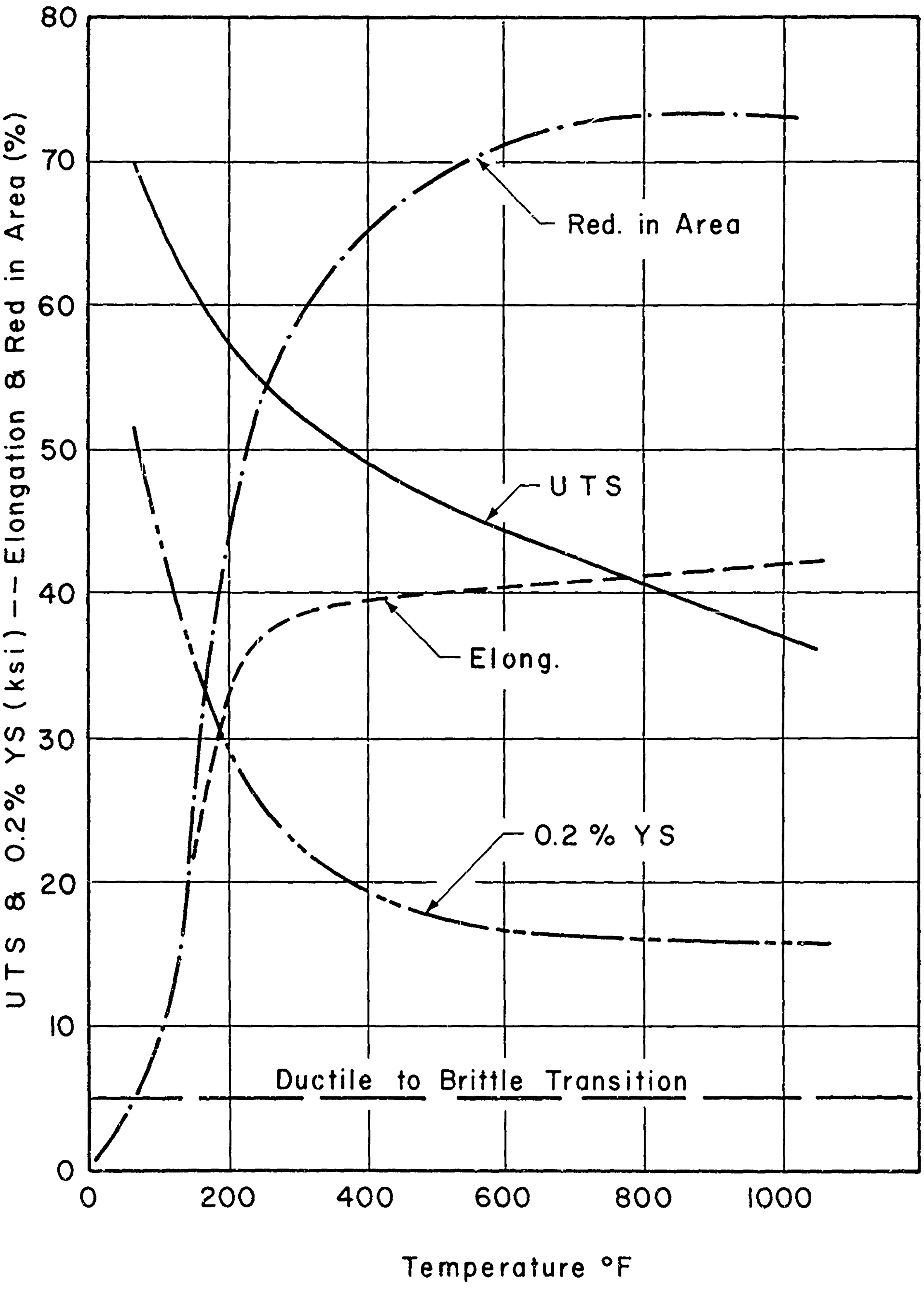


Figure 140 - Tensile properties of high-density, pressed and sintered molybdenum.

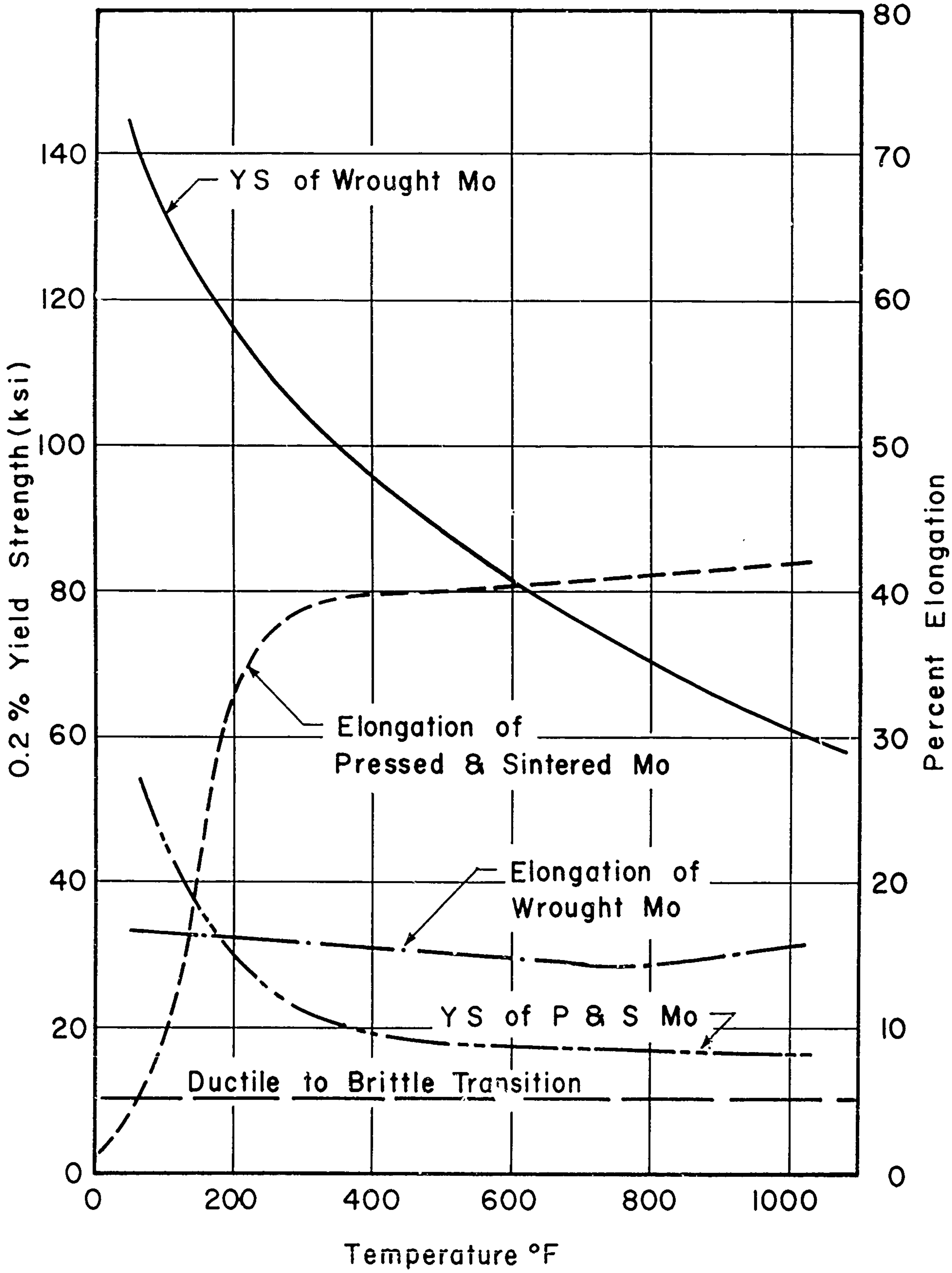


Figure 141 - Comparison of the tensile properties of wrought molybdenum (0.635" diameter rolled bar) with those of high-density, pressed and sintered molybdenum.

Table IL--Short Transverse Tensile Properties of Extruded Molybdenum at Two Strain Rates as a Function of Temperature

Temperature (°F)	Percent Elongation		0.2% YS (ksi)		UTS (ksi)	
	0.02 in/in/min	0.2 in/in/min	0.02 in/in/min	0.2 in/in/min	0.02 in/in/min	0.2 in/in/min
72	0.06-0.14	(a)	(b)	(a)	85.90-90.40	(a)
150	0.996	(a)	80.50	(a)	84.80	(a)
250	2.24	(a)	70.00	(a)	76.60	(a)
400	17.90	1	67.60	86.75	72.50	88.05
500	(a)	4	(a)	75.00	(a)	77.55
600	12.70	21	62.46	69.05	66.53	71.30
800	9.58	(a)	60.90	(a)	64.12	(a)
1000	14.56	(a)	59.31	(a)	61.20	(a)

(a) Not tested

(b) Brittle failure--did not yield

The random brittle failures seen in 80-20, pressed and sintered tungsten, and pressed and sintered tungsten-2% thoria, are the result of insufficient ductility in every direction. Figure 142 illustrates the tensile properties of 80-20 as a function of temperature. Based on the arbitrary criterion, its ductile-to-brittle transition temperature would appear to be approximately 250°F. Based on the same criterion, the ductile-to-brittle transition temperatures of pressed and sintered tungsten and tungsten-2% thoria would appear to be 800°F and 1000°F, respectively. Based on the experience gained under this contract, a higher ductility standard (e.g., 10%) would seem appropriate.

The most striking example of plastic deformation of a cavity was provided by the H-13 insert, which apparently underwent a dramatic reduction of hardness in service. (See Figure 116.) Examples of gaps at interfaces and dented parting surfaces may be seen in Figure 134. High-density, pressed and sintered molybdenum Inserts 2 and 6 and the silicided, wrought molybdenum Insert 4 provide striking examples. The relatively low strength of pressed and sintered molybdenum (see Figure 140) made it particularly prone to this mode of deterioration. The stronger die materials, such as Mo-3, HOT SHOT 2920X, TZM, and wrought molybdenum, were much less prone to suffer plastic deformation. This is made quite obvious by comparing the gap measurements made in the high-density, pressed and sintered molybdenum insert retainer plates after 3000 shots (Table XXXV) with the gaps measured in the wrought molybdenum insert retainer plates after 2585 shots (Table XLIII).

Refractory metal dies are quite resistant to being soldered by AISI 304 stainless steel, and soldering was only encountered in the Lamp Metals and Components Department's materials evaluation die when flashing was experienced accidentally. The very high velocity of the flash apparently works to remove the boundary film between the molten metal and the die and/or raises the surface of the die to very high temperatures. Whatever the mechanism, soldering seems to be the inevitable result.

Three materials were found to be extremely susceptible to erosion--copper-infiltrated molybdenum, Anviloy 1150, and GE-474. The Anviloy 1150 appeared to suffer mechanical erosion, i.e., fragments from the badly heat checked surface were apparently washed away by the metal stream. (See Figure 133.) The GE-474, on the other hand, seemed to be susceptible to chemical erosion, i.e., it dissolved in the cast metal. The chemical erosion appeared to act preferentially on heat checked surfaces, which may well have experienced the highest surface temperatures. The result was to produce round-bottom heat checks of truly unusual appearance. (See Figure 122.) The surface damage suffered by the copper-infiltrated molybdenum was so swift and dramatic that it could not be attributed with confidence to either one or the other of the two modes of erosion identified.

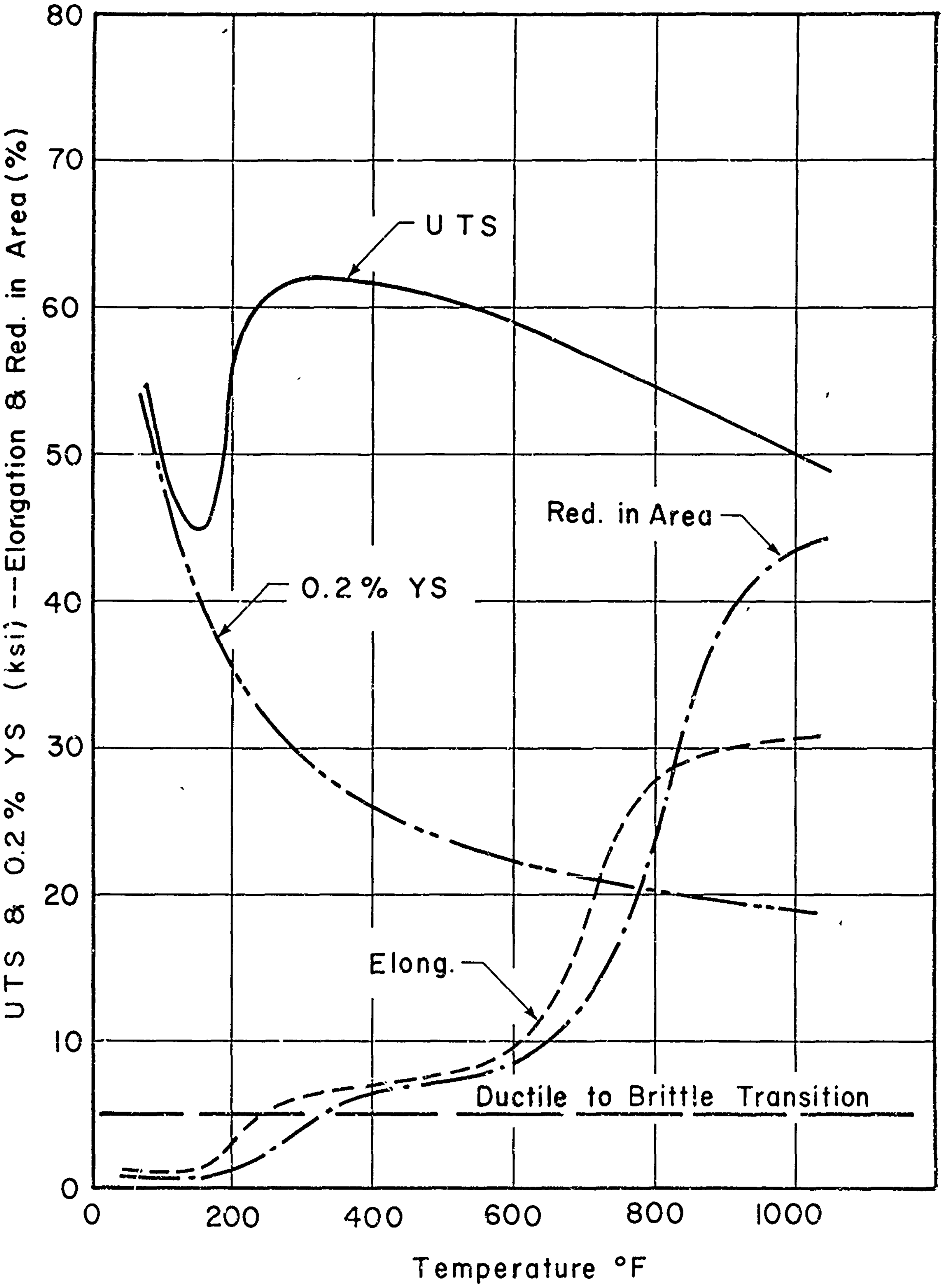


Figure 142 - Tensile properties of the pressed and sintered 80% molybdenum, 20% tungsten binary alloy (80-20).

Pitting was observed only in TZM and Mo-3. The appearance of the phenomenon suggested that it was the result of the preferential erosion of one or more microconstituents from the surface of the die. Several other general comments concerning the die materials evaluation seem appropriate. First, it was found impossible to discriminate between those inserts which were annealed and those which were not. Insert 4, in Figure 134 (high-density, pressed and sintered molybdenum), and Insert 1, in Figure 135 (silicided, wrought molybdenum), were both stress relief annealed. Nevertheless, the tool marks left in the half-round sections of both of these inserts by the ball end mill served as sites for the initiation of cracking. When repairing the wrought molybdenum, cover-half insert retainer plates, an end mill was brought into contact with the runner system to establish a reference level. That plate was not subsequently annealed, and the reference point became a site for accelerated heat checking and the source of a crack that ran into a nearby insert pocket. Similarly, a small hairpin-shaped scratch across the same runner also served as a site for the initiation of a crack. Both of these defects can be seen on the left-hand side of Figure 134, in the runner system, approximately 1" and 2" from the shot sleeve hole, respectively. One obvious conclusion is that molybdenum dies should be polished to eliminate tool marks. One is also lead to suspect, again, that molybdenum is very notch sensitive.

Another general observation that was made was that the environment in the cover half of the materials evaluation die was not so challenging as that in the ejector half. Perhaps the most concrete evidence for that allegation was the substantially poorer performance of Anviloy 1150 as an ejector-half insert than as a cover-half insert. If the observation is indeed valid, the relative rankings of TZM and Mo-3, among others, may have to be revised downward; and the rankings of HOT SHOT 2920X and wrought molybdenum, among others, may have to be revised upward.

Another almost intuitive impression concerning the effect of location in the materials evaluation die is seemingly verified by Tables XXXIII and XXXV. That impression is that gap formation proceeded more swiftly at those interfaces closer to the sprue than at those interfaces more distant from the sprue. The variations in yield strength represented by the various insert materials make this impression most difficult to verify.

SECTION VIII

PILOT CASTING OPERATION

The purpose of this phase of the project was to apply the experience and knowledge gained in the earlier phases of the contract to assemble and operate the first, semi-production ferrous die casting facility in the United States. This pilot operation was intended to produce:

a. Data for the final selection of a die material

b. Parts for testing by the Air Force

c. A suggested operating procedure for ferrous die casting

d. Data relative to the process economics.

The detailed analysis of the economics of the ferrous die casting process is presented in Section IX.

1. PROTOTYPE ANALYSIS

The part selected by the Air Force to demonstrate the capabilities of the ferrous die casting process on a semi-production scale was a hollow hemisphere, which was then being purchased as an aluminum die casting from several suppliers, including the Doehler Jarvis Division of the National Lead Company. Two restrictions were imposed on the ferrous die casting:

a. It was to have the same external configuration and dimensions as its aluminum counterpart

b. Its weight was to be adjusted so that the total weight of the device into which it was to be incorporated would remain unchanged, regardless of the material from which the hemisphere was cast.

Inasmuch as iron and steel are approximately three times as dense as aluminum, these restrictions meant that the wall thickness of the ferrous hemispheres would have to be less than that of the aluminum hemispheres. The ferrous metal shell, however, was expected to possess a greater unit rupture stress than the aluminum alloy used in the standard device, the difference in strength being a function of the alloy selected. These counter trends made it unclear what the net effect of substituting a ferrous hemisphere for an aluminum hemisphere would be on the performance of the device.

To better define the performance that could be expected from a die cast iron or steel hemisphere, a number of prototypes were produced. Aluminum hemispheres, supplied by Doehler Jarvis, were modified to form thin-walled patterns with two different internal configurations, one smooth and one grooved. The General Electric Company Manufacturing-Engineering Service added a 3/8" diameter gate extension to the top of each pattern along its axis of rotation, cast plaster match plates for each pattern, and prepared ceramic molds for the precision casting of the prototype hemispheres.

The prototypes were made in clusters of four per mold from 2.71% CE malleable iron and from AISI 1060 steel melted in a 25 pound, rollover arc furnace. Only one casting was made from each 5.5 pound melt, however. The nominal composition of the two alloys was:

	C	Si	Mn	S	P	Fe
2.71% CE malleable iron	2.30%	1.24%	0.33%	0.010%	0.10%	bal
AISI 1060 steel	0.60%	0.75%	1.00%	0.010%	0.011%	bal

Just prior to pouring, 0.15% calcium-silicon, 0.15% manganese, and 0.1% aluminum were added to the steel melt for the purpose of degassing. Both alloys were teemed into molds at pouring temperatures in the range of 2800°F to 2900°F. The molds were preheated, but only to a temperature less than 200°F. The teemed molds were immediately placed in a reducing atmosphere of hexamine to prevent scaling.

The castings were removed from the mold and given a coarse grit blast to eliminate remnants of the mold material. The gates were broken off the iron castings and cut off the steel castings. The individual castings were then sealed in stainless steel foil envelopes for heat treatment. Pieces of carbon were put in the envelopes with the cast iron hemispheres to prevent decarburization.

The annealing treatment employed to convert the white iron castings to ferritic malleable iron was:

a. Heat rapidly to 1680°F (the actual time required to raise the temperature from 1000°F to 1680°F was one hour)

b. Hold at 1680°F for 24 hours

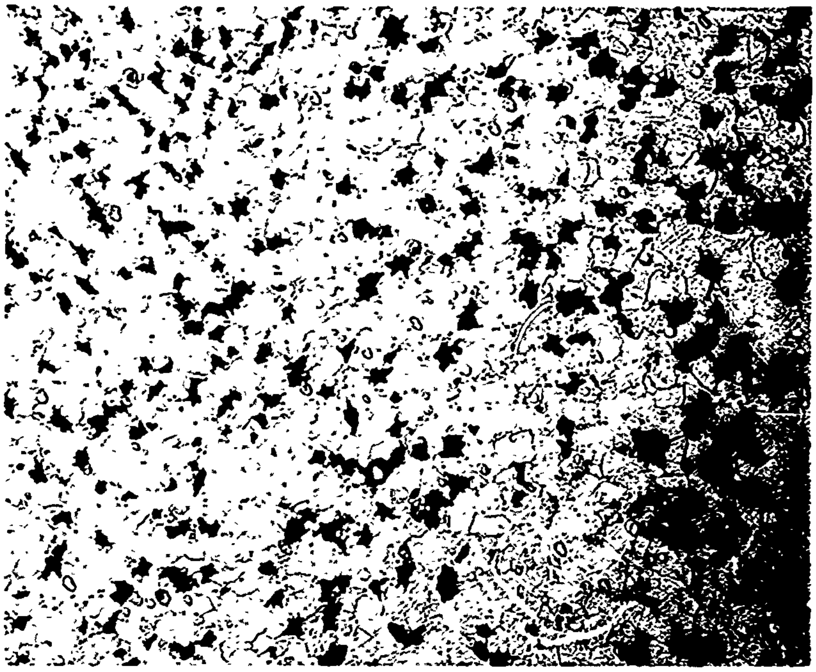
c. Cool rapidly to 1400°F (the actual time required to cool from 1680°F to 1400°F was 1/2 hour)

d. Cool from 1400°F to 1280°F at 8° per hour

e. Cool in still air from 1280°F to room temperature.

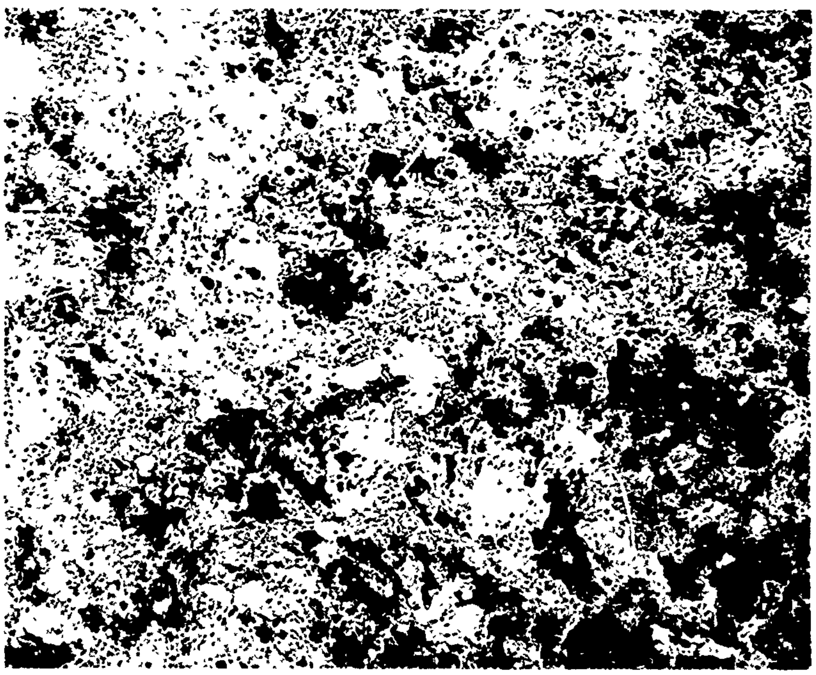
Figure 143 indicates the structure that was achieved.

The AISI 1060 steel castings were annealed by holding them for one hour at 1670°F and then furnace cooling to develop a pearlitic structure. (See Figure 144.)



Etchant: Nital Mag. = 100X

Figure 143: Microstructure of an annealed 2.71% CE malleable iron hemisphere, precision cast in a ceramic mold.



Etchant: Nital Mag. = 100X

Figure 144: Microstructure of an annealed AISI 1060 steel hemisphere, precision cast in a ceramic mold.

After annealing, the vestigal gates were removed by hand grinding, and the castings were recleaned by blasting with 180 mesh aluminum oxide grit. The spherical diameters of the castings were measured immediately above the locking flange and compared to the same dimension on the aluminum patterns. The AISI 1060 steel castings were found to have undergone an average shrinkage of 0.020" per inch, whereas the 2.71% CE malleable iron castings only underwent an average shrinkage of less than 0.005" per inch. (The latter value may be compared with an average shrinkage of 0.008" per inch, and a standard deviation of 0.005" per inch, measured across the flange diameter of the 3.1% CE iron die castings produced by Doehler Jarvis and malleablized by Dort.)

The major inconsistency between the precision cast prototypes of the same material was an across-the-parting-line dimensional deviation which was manifest as a thickness variation in the locking flanges. The flange thickness was measured for several of the prototypes and was found to be up to 0.010" greater than the nominal thickness. It was calculated that each 0.010" increase in flange thickness would increase the weight of a hemisphere by 5.9 grams.

The grooved hemispheres only were weighed before shipment. The results of that weighing are compiled in Table L, which indicates that the steel hemispheres were heavier than the malleable iron hemispheres (this was to be expected, because of the difference in density between the two alloys) and that the tops were heavier than the bottoms. The tops were also more variable in weight than the bottoms; the significance of that observation is not clear.

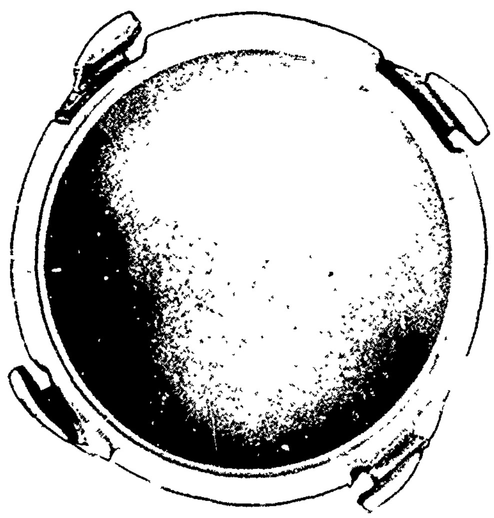
Visual inspection revealed hot tearing only in the AISI 1060 steel hemispheres. In the configuration with the smooth ID, the hot tears appeared at the intersection of the radial flute surface and the outer diameter of the hemisphere, as illustrated by Figure 145. In the configuration with the grooved ID, hot tears and pin holes appeared in the bottoms of the grooves at those points at which they were overlaid by the flutes. These defects can be observed in Figure 146, which illustrates the two configurations evaluated by the Air Force. Both of the manifestations of hot tearing noted above were related to solidification shrinkage in the flutes that could not be fed from the single, polar gate with which the castings were provided. The porosity resulting from that shrinkage may be seen in Figure 147, which is a macrograph of a section made about 3/8" above the base of the hemisphere, parallel to the base.

On the basis of an evaluation of the performance of the four prototype combinations, i.e., malleable iron hemispheres with smooth and grooved inner diameters and 1060 steel hemispheres with smooth and grooved inner diameters, the Air Force selected malleable iron hemispheres with smooth inner diameters for pilot production by ferrous die casting.

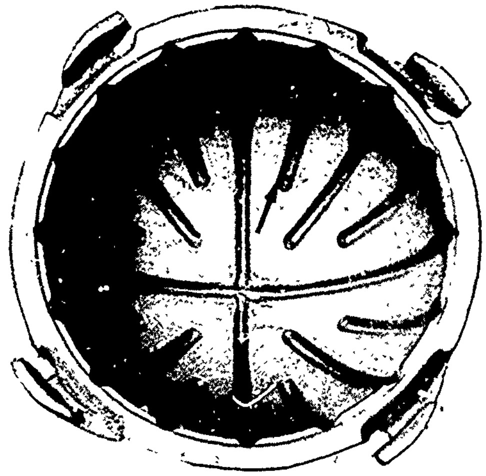
Table L--Weight Distribution Statistics for Precision Cast, Grooved Prototype Hemispheres

	Weight (grams)					
	2.71% CE Malleable Iron			AISI 1060 Steel		
	Tops	Bottoms	Pooled	Tops	Bottoms	Pooled
Arithmetic mean	127.28	122.75	250.03	124.33	121.53	245.85
Standard deviation	1.7803	.93275	1.4213	1.4627	0.71723	1.1519

Figure 145: Hot tears in the AISI 1060 steel prototype hemispheres, precision cast with smooth inner diameters.



Smooth Inner Diameter



Grooved Inner Diameter

Figure 146: AISI 1060 steel hemispheres illustrating the two internal configurations evaluated in the precision-cast prototypes--note the pin hole and the hot tear in the grooves of the casting to the right.

Etchant: 2% Nital Mag. = 5X

Figure 147: Macrograph of a section parallel to the base of a precision cast AISI 1060 steel prototype hemisphere revealing the shrinkage porosity in the flute.

2. DESIGN PHASE

The design phase of the pilot production effort under this contract included the selection of an injection system from the several investigated, the design of the die incorporating all of the knowledge and art available at that point in time, and the selection of die materials, based on the earlier die materials evaluation.

The investigation of injection systems for horizontal die casting machines was discussed at length in Section III of this report. As a result of that investigation, Dort concluded that either a finned, air-cooled shot sleeve or a heated composite shot sleeve could be relied upon to maintain the nearly isothermal conditions required to make a precision injection system with close tolerances possible. Although the alumina-lined shot sleeve also looked promising, sufficient experience had not been gained with that sleeve concept to ascertain its reliability. The spatial requirements of the finned, air-cooled shot sleeve were not entirely compatible with the 400 ton Lester horizontal die casting machine which Dort planned to employ for the pilot casting operation (primarily because the Lester machine features a heavy cast frame). The elimination of the ceramic-lined and the finned, air-cooled shot sleeves left the heated, composite shot sleeve as the only proven and practical injection system for the pilot casting operation.

Figure 29 indicated the design of the composite shot sleeve evaluated in the simulated die casting machine. As can be seen in Figure 148, a number of significant changes were embodied in the sleeve designed for the pilot casting operation. Under the assumption that the relatively low thermal conductivity of the René 41 impeded the transfer of heat into the beryllium-copper heat transfer medium, the thickness of the liner was reduced from 1/4" to 1/8". To avoid exposure to liquid metal at the interfaces between the three concentric sleeves that together form the composite shot sleeve, the René 41 liner was designed with a flange which covered the interfaces between the three concentric sleeves and, seated on a circumferential shoulder, recessed in the face of the cover die. Safety grooves were introduced where the runners traversed the interface between the shot sleeve flange and the die. (The safety groove concept is illustrated by Figure 80.)

An H-13 outer shell was used on the composite shot sleeve in the preliminary investigation on the simulated die casting machine. The H-13 was replaced in the pilot casting operation with an AISI 304 stainless steel outer shell which, it was felt, would have several advantages. The coefficient of thermal expansion of 304 stainless steel is more nearly like those of René 41 and beryllium-copper than is that of H-13. The oxidation resistance of 304 stainless steel is much superior to that of H-13. Because its thermal conductivity is relatively low, compared with that of H-13, 304 stainless steel was also preferred. The low conductivity of

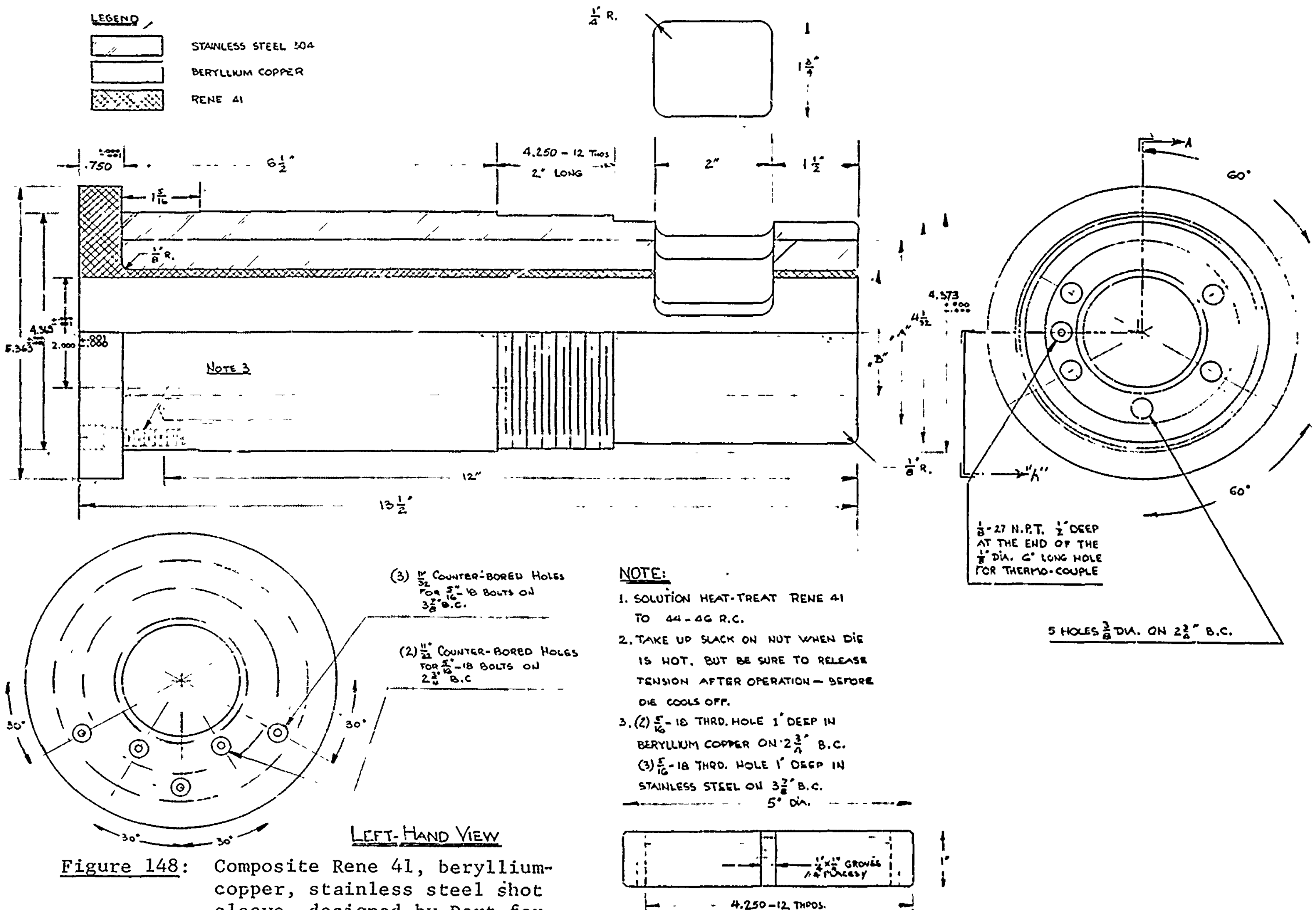


Figure 148: Composite Rene 41, beryllium-copper, stainless steel shot sleeve, designed by Dort for the pilot casting operation.

the 304 stainless steel was expected to reduce the heat loss to the stationary platen of the die casting machine and thus restrict the temperature rise of the platen; and, by reducing the heat losses to the platen, the stainless steel shell was expected to promote axial and circumferential heat transfer through the beryllium-copper and thus assist the establishment of isothermal conditions in the shot sleeve.

Previous ferrous die casting experience had suggested that mechanical flexure of refractory metal dies should be avoided. One source of such flexure is the force imparted to the cover half of the die through the shot sleeve when biscuits and/or the plunger become stuck in the shot sleeve. To eliminate that source of potential flexure, the Lamp Metals and Components Department had recommended that the injection system be designed so that the axial thrust transmitted to the shot sleeve from the injection cylinder would be borne by the stationary platen. Dort determined that such an arrangement was impractical on the Lester machine but, as a compromise, they were able to distribute the load over a large area on the back of the top clamping plate.

The die for the pilot casting operation was designed by the Doehler Jarvis Division, in consultation with the Lamp Metals and Components Department. The considerations given the greatest weight in the design of the die were the desire to attain long die life and trouble-free performance. Doehler Jarvis' ferrous die casting experience had indicated that interfaces between die components were potential sources of difficulty and delay, which should be eliminated whenever and wherever possible. The application of that precept led Doehler Jarvis to design a die which, like their test bar die, employed only a single refractory metal impression block in each half of the die. Conceptually, that decision imposed no limitations on the design of the die but, on a practical level, it was very restrictive. Stated simply, the difficulty was that refractory metals are not generally available as large blocks, primarily because there was very little demand for such blocks before the advent of ferrous die casting. Indeed, the size indicated for the impression blocks in Doehler's preliminary design had to be reduced, because it exceeded the supplier's capabilities.

Having decided to employ a single-piece die, and having determined the maximum size to which the impression blocks can be produced, the designer should presumably be able to proceed to maximize the utilization of the available area within the limits imposed by the method of liquid metal transfer (i.e., manual or automatic) and the injection pressure and lock-up rating of the die casting machine. (The lock-up rating of the machine must exceed the product of the projected area of the entire gate multiplied by injection pressure by a substantial margin, if close tolerances are to be maintained across the parting line and flashing is to be avoided.) For this project, however, it was impractical for Doehler Jarvis to follow that procedure exactly.

The single-machine pilot casting operation was to produce pairs of hemispheres which were not identical but, essentially, mirror images of one another. Squeezing a third hemisphere into the available space, therefore, would have been an exercise in futility, proving nothing but increasing die construction and metal costs.

In the interest of controlling project costs and avoiding potential problems which might have jeopardized the successful completion of the project, therefore, Doehler Jarvis designed a two-cavity die. (Figures 149 through 153 illustrate the final design of that die.) Nevertheless, everyone concerned with the project recognized the inefficiency a two-cavity die represented, with respect to the utilization of both the die casting machine and the operating personnel.

Soldering was another major problem experienced by Doehler Jarvis in their exploratory ferrous die casting effort. It was found to be particularly troublesome during the production of eutectic and malleable iron castings. Some of the sources of soldering that were identified were high pouring temperature, turbulent flow, and high injection velocity. For the pilot production die, the "self-cleaning" joint, which produced a great deal of turbulence, was forsaken in favor of the safety groove, which produced virtually no turbulence. To permit a metal to be cast at a temperature close to its liquidus, it was necessary to minimize the loss of sensible heat that the metal would experience in the runner system. Doehler took one step in that direction by minimizing the length of the runners in the pilot production die design. The heat loss in the runners could be further reduced by reducing the injection time. To reduce the injection time by increasing the injection velocity, however, would have been self-defeating, inasmuch as high injection velocities, too, promote soldering. The design solution Doehler proposed for this dilemma was to increase the cross-sectional areas of the runners and gates so that high mass flow rates and short injection times could be attained at low injection velocities. (Compare Figure 152 with Figure 75 for an indication of the evolution in Doehler's thinking.)

The experience of the Lamp Metals and Components Department had indicated that, at high production rates, extraction of heat from a die being used for ferrous die casting could be a problem. The concomitant problem was to prevent significant amounts of that excess heat from being transferred to the hydraulic ejection mechanism, the machine platens, the tie bars, or the machine frame. Doehler provided water cooling in their die design. They also specified the use of insulating board between the bottom plate of the mold base and the moveable platen,

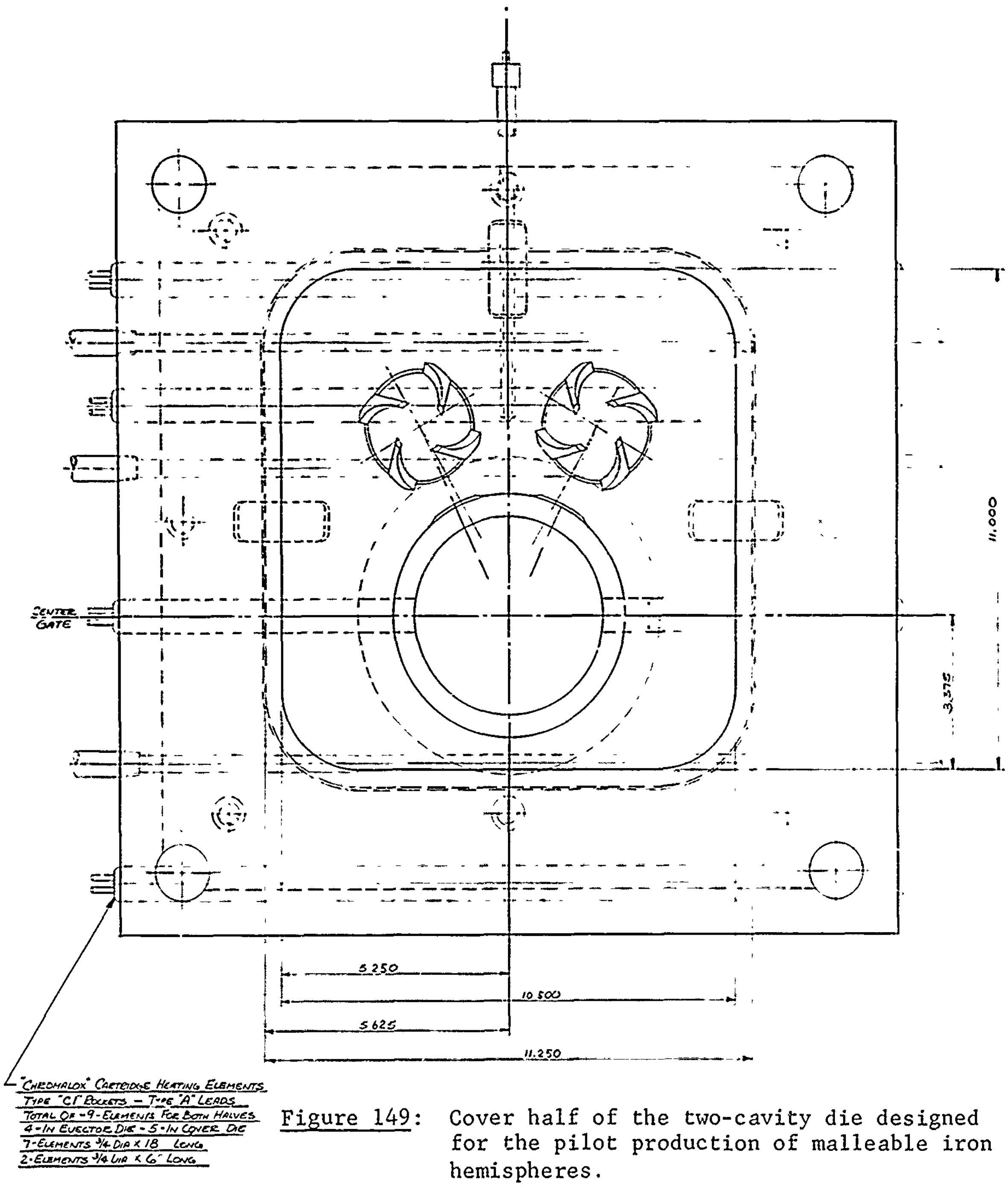


Figure 149: Cover half of the two-cavity die designed for the pilot production of malleable iron hemispheres.

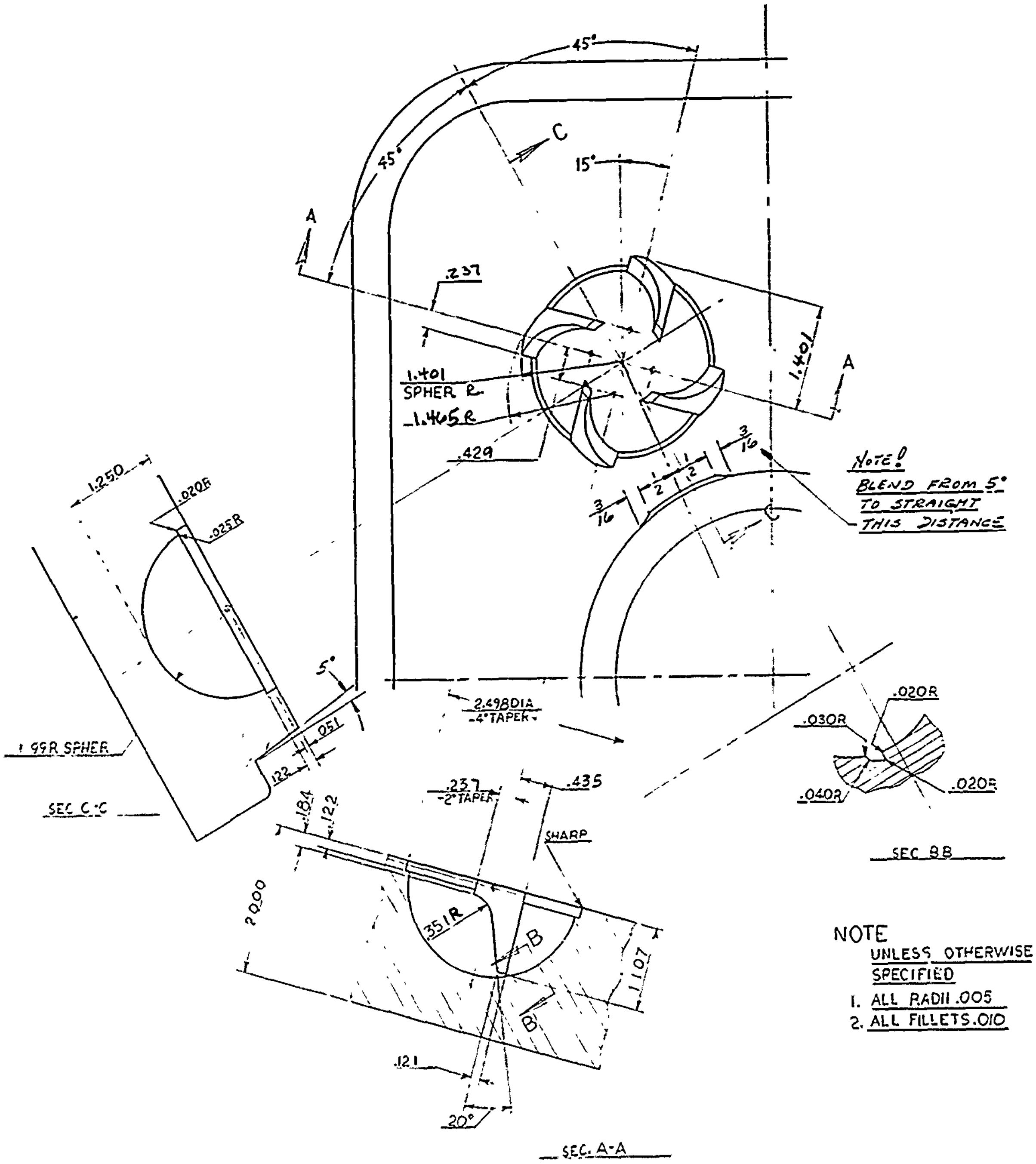


Figure 150: Details of construction of the cover die for the pilot production of malleable iron hemispheres.

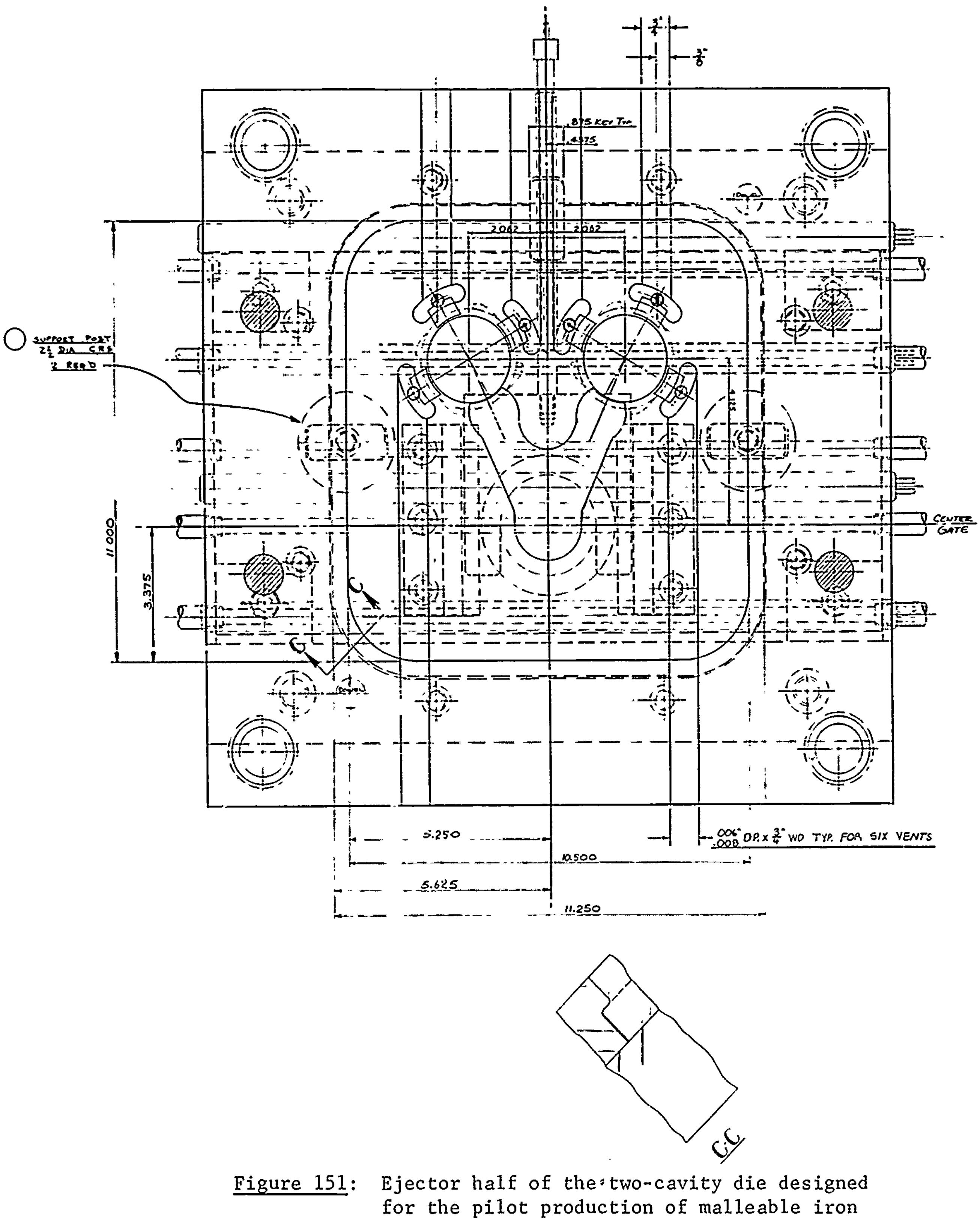


Figure 151: Ejector half of the two-cavity die designed for the pilot production of malleable iron hemispheres.

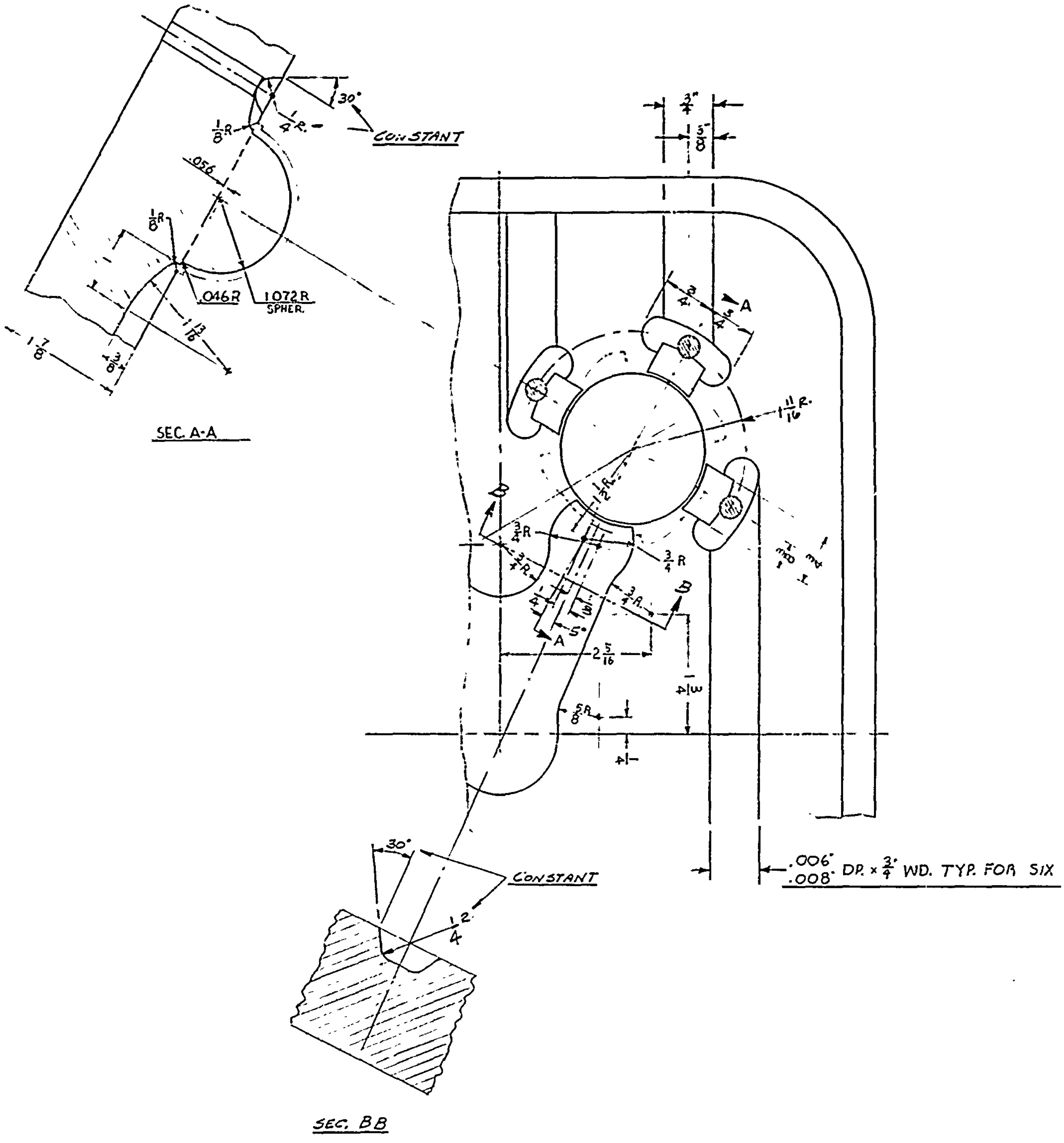


Figure 152: Details of construction of the ejector die for the pilot production of malleable iron hemispheres.

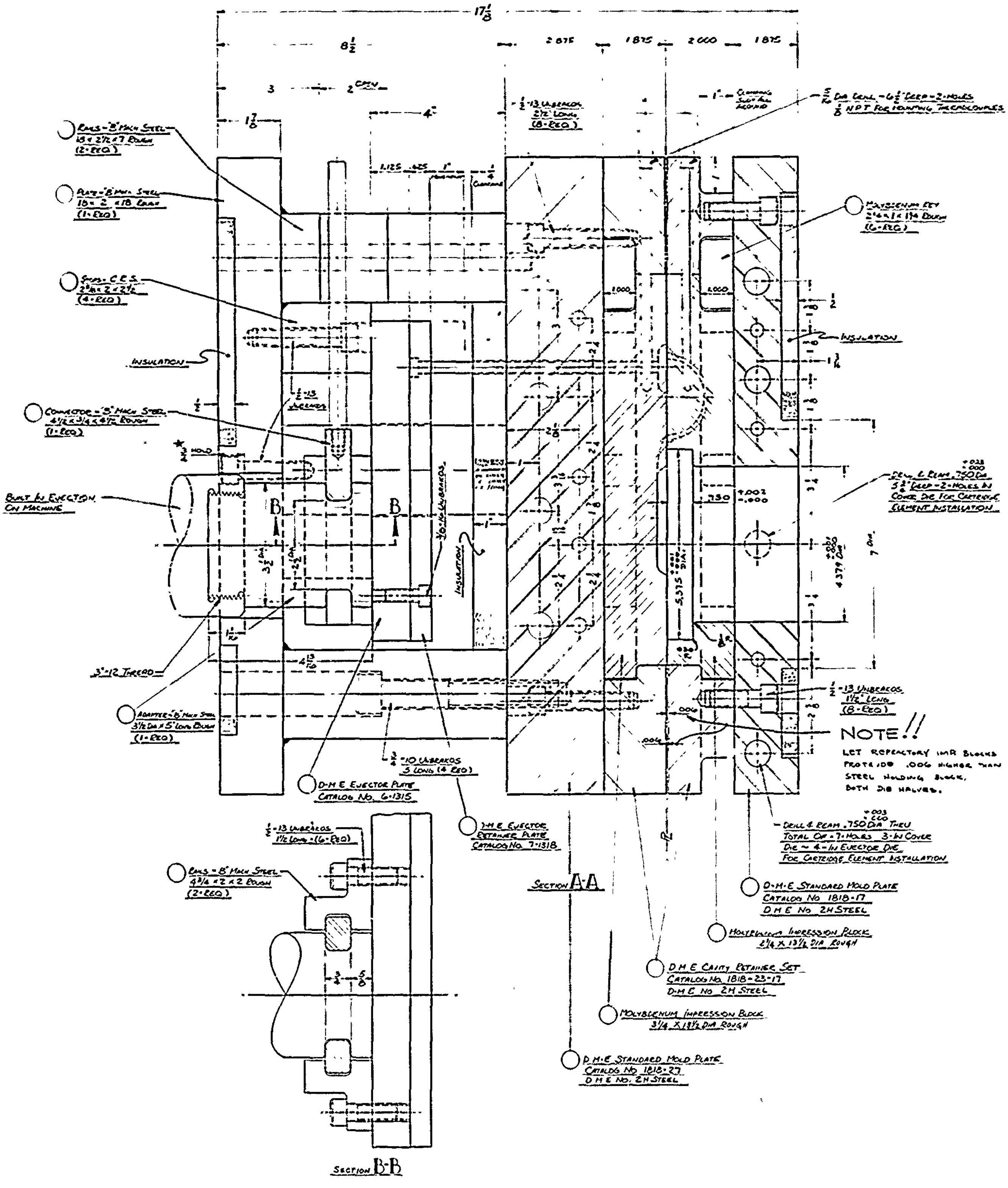


Figure 153: Sectional view of the two-cavity hemisphere die designed for the pilot production of malleable iron hemispheres.

between the top clamping plate and the stationary platen, and between the support plate and the ejector retainer plate. In the first two locations, the insulation was to be recessed in the mold base plates, so that it would be completely contained and would be required to support only minor loads. Such precautions were unnecessary between the support plate and the ejector retainer plate, where the insulation was only required to protect the hydraulic ejection mechanism from convective and radiant heat transfer. The insulation in that location was simply to be fastened to the back of the support plate. (See Figure 153.)

The Lamp Metals and Components Department had also observed that not only did ejector pin holes serve as sites for the initiation of cracks, but that the problem was most severe in the runner system. For that reason, the ejector pin holes in the pilot production die were located only in the overflows and were specifically excluded from the sprue, runners, and cavities. Inasmuch as Doehler Jarvis, Dort, and the Research and Development Center had all successfully used steel ejector pins in refractory metal dies, refractory metal ejector pins were not specified.

Several of the design features of the pilot production die were incorporated, specifically to accommodate the difference in thermal expansion between the refractory metal impression blocks and the steel components of the mold base. The refractory metal impression blocks were made 0.006" thicker than the A and B plates, so that they would be flush with the parting surface of the A and B plates at operating temperature. Recesses were machined in the back of the A and B plates to accept the shoulders on the back of the impression blocks. This made it possible to securely retain the impression blocks, without restricting their freedom to expand and contract independently from the rest of the die. To maintain registry across the parting face under such circumstances, the geometric center of the impression blocks was arbitrarily selected to define a fixed reference axis, around which relative movement of the mold base and impression blocks could occur. That axis was fixed by providing molybdenum keys to engage slots milled into the back of the A and B plates and the impression blocks, along the vertical and horizontal centerlines of the impression blocks. Only three keys were required in each half of the die. Their locations can be seen in Figures 149 and 151.

Support pillars were added to the design of the pilot production die as insurance against flexure of the ejector die and the refractory metal ejector impression block during metal injection. The argument in favor of such a precaution is, essentially, the same as that used to justify locking the shot sleeve to the stationary platen, i.e., it reduces the possibility of die life being adversely affected by mechanical fatigue. The increased rigidity of the ejector die may offer the additional advantage of producing parts closer to tolerance, with less

across-the-parting-line dimensional variance. Figure 151 indicates the location selected for the support pillars. That element of the design, however, was inadvertently omitted in die fabrication. No adverse effects were noted.

Doehler Jarvis produced some die cast ferrous hemispheres in an impression without vents. Radiographic examinations of those castings indicated that, in terms of porosity, they were indistinguishable from the castings produced in vented impressions under the same conditions. Therefore, Doehler provided no vents in their preliminary design for the pilot production die. As a concession to conventional practice, however, flat vents were added to the die during fabrication. The vents communicated only with the overfl ws, each of which was independently vented. To avoid the possibility of the operator or an observer being struck by flash, all of the vents were directed either straight up or straight down. (See Figures 151 and 152.)

Although slots are cast in the flutes of the production aluminum hemispheres, the cores which produce those slots are subject to early failure. During their exploratory ferrous die casting work, Doehler had also observed the early failure of the projecting lip, provided on one of the high-density, pressed and sintered molybdenum ejector impression blocks, to form a circumferential channel or groove in the mating surface of the cast hemispheres. (Refer to Figure 81.) Any thought of casting the slots in the flutes of the malleable iron hemispheres was dismissed, therefore, and a machining operation was scheduled to produce the slots in the annealed hemispheres.

In the die cast ferrous hemispheres, which they produced during their exploratory work, Doehler had increased the wall thickness near the base by adding an internal skirt. That permitted them to incorporate the same tongue-and-groove mating configuration employed in the production aluminum hemispheres. The same approach could not be employed in the pilot casting operation, because it produced unacceptably heavy castings. To resolve that dilemma, it was decided that the mating surfaces of the castings produced in the pilot production run should be shouldered. The tops were to have an internal shoulder and the bottoms an external shoulder. Because a machining operation was required to provide the slot through the flutes of the hemispheres, it was concluded that it would be most practical to cut the shoulders into the bases of the hemisphere simultaneously. This decision permitted the top and bottom hemispheres to be cast with identical mating surface configurations. The required machining operations are illustrated by Figure 154.

The wall thickness of the hemispheres was based on the calculated weight requirement for the part. The specified radii were a compromise between the desire to maintain radii as small as those produced in the aluminum die castings and the even greater desire not to employ radii so small that they might serve as sites for the initiation of cracks. Figure 155 presents the dimensions which were established as a target for the pilot production run, as a result of the noted consideration.

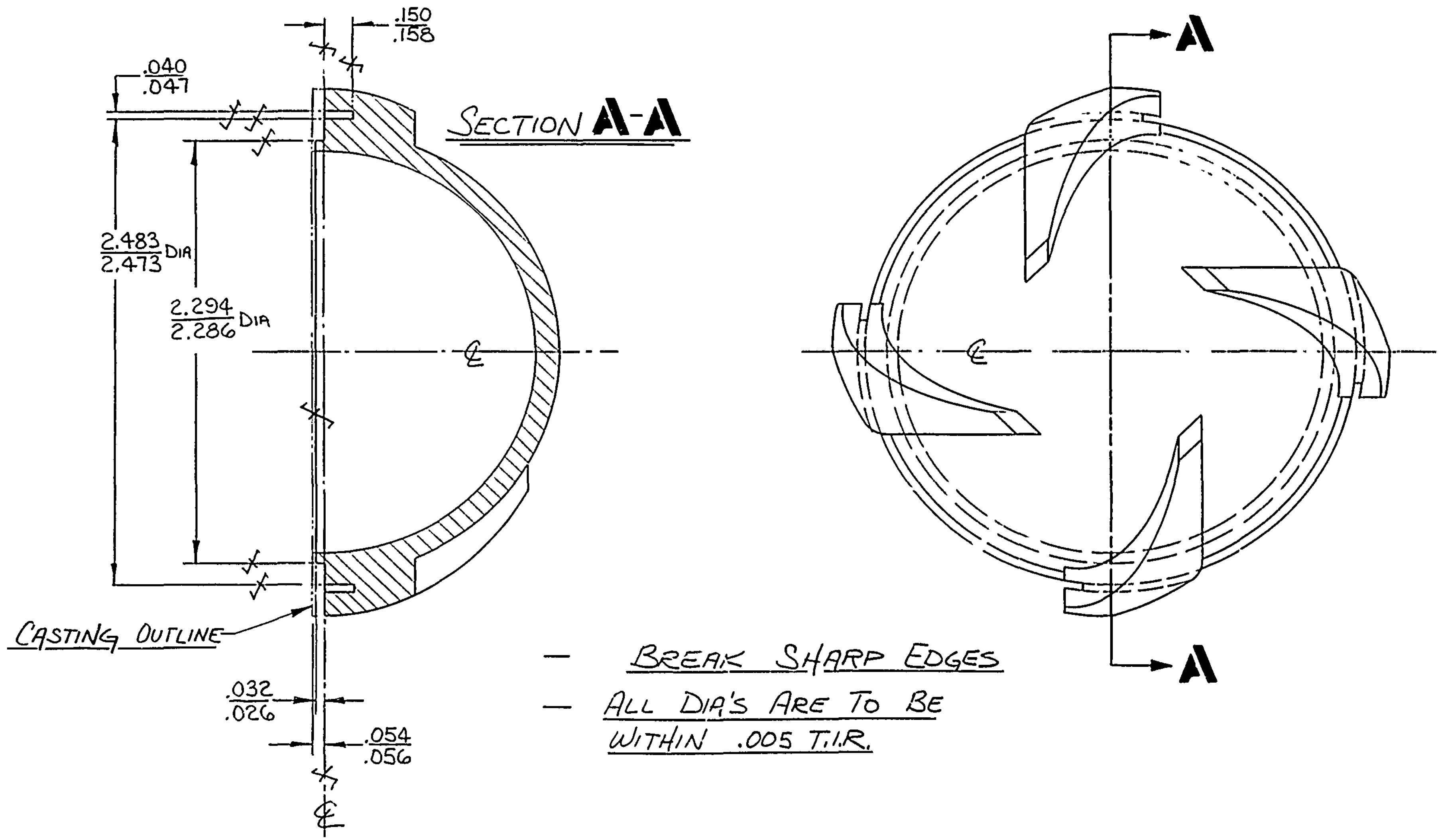


Figure 154: Drawing of the bottom hemisphere indicating the machining operations required to prepare the pilot production castings for testing.

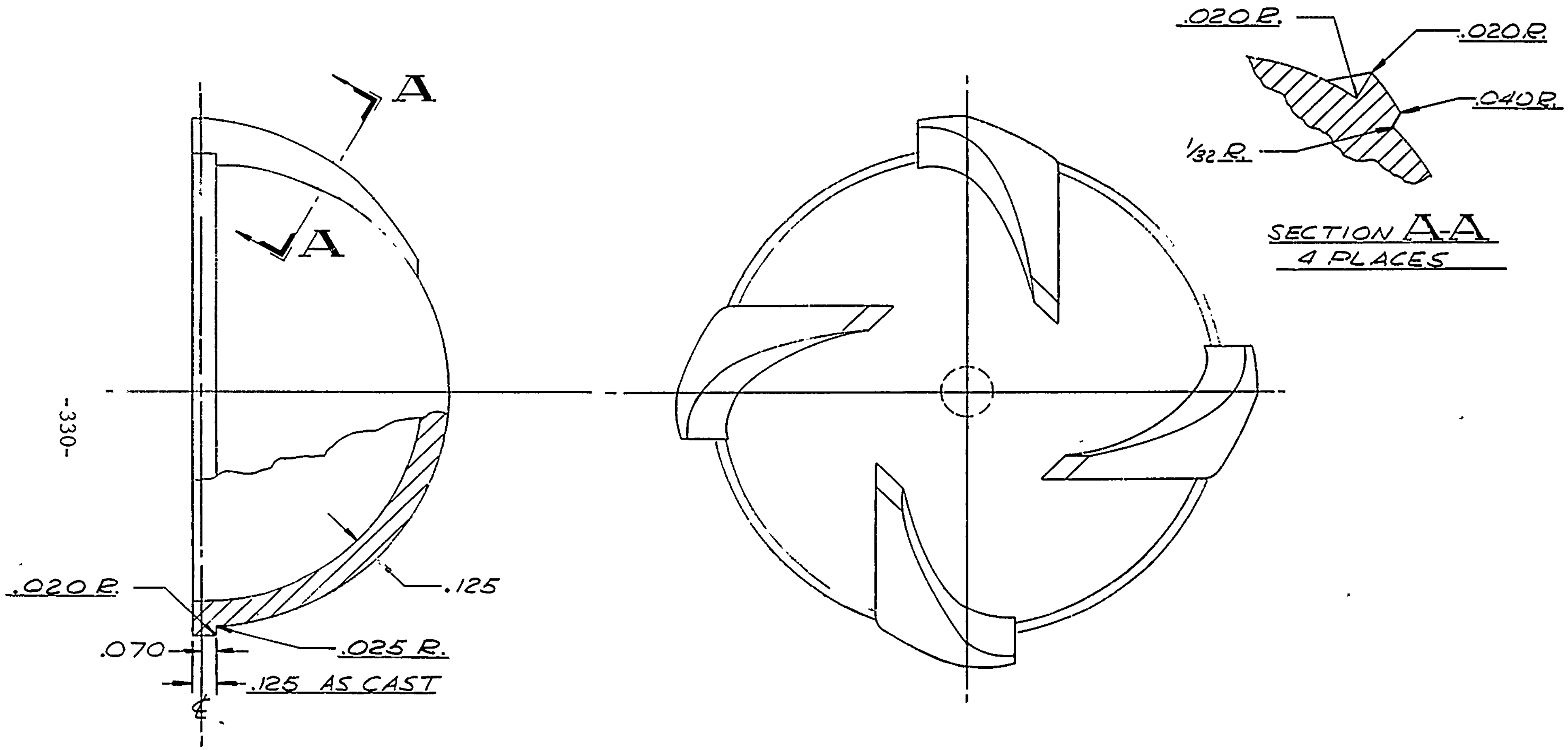


Figure 155: Drawing of the top hemisphere indicating the desired as-cast dimensions of the pilot production castings.

To determine the shrinkage allowances that had to be applied to the die cavity to produce castings to the desired dimensions, Doehler Jarvis carefully measured the height and flange diameter of a number of the die cast 3.1% CE iron hemispheres which they had produced. The castings were subsequently subjected by Dort to a ferritizing, batch anneal and remeasured. Inasmuch as the cavities in which these castings were made were not identical, the data were normalized to reflect a change of length per unit length. On the average, the flange diameter of the castings was 0.0088" per inch less than the flange diameter of the cavities with a standard deviation of 0.0013" per inch; the corresponding value for the hemisphere height was 0.0115" per inch, with a standard deviation of 0.0024" per inch. After annealing, the flange diameter was, on the average, 0.0081" per inch smaller than the cavity (with a standard deviation of 0.0052" per inch) and 0.00073" per inch smaller than in the as-cast condition (with a standard deviation of 0.0047" per inch). The height of the hemisphere after annealing was, on the average, 0.0146" per inch smaller than the cavity (with a standard deviation of 0.0033" per inch) and 0.0032" per inch smaller than in the as-cast condition (with a standard deviation of 0.0019" per inch). The required flange diameter was 2.472" to 2.483", and a shrinkage allowance of 0.020" was added to the cavity design. The required height of the hemisphere was 1.250" to 1.258", and a shrinkage allowance of 0.015" was added to the cavity design. (Recalculation indicates that 0.0183" would have been a better value.) Detailed cavity dimensions can be found in Figures 150 and 152.

The original intention had been to conduct a more detailed evaluation of the four most promising die materials, in conjunction with the pilot casting operation. The decision to employ a two-cavity die with single-piece impression blocks meant that a maximum of two die materials could be evaluated concurrently. In view of the delays and unexpectedly high costs experienced, the die materials evaluation requirement was relaxed from four to two. Consideration was limited to commercial materials which could be readily obtained in the desired sizes. Based on the evaluation of die materials conducted by the Lamp Metals and Components Department, the two most promising commercially available die materials were TZM and high-density, pressed and sintered molybdenum.

High-density, pressed and sintered molybdenum was selected for the ejector half impression block for two reasons, basically:

a. High-density, pressed and sintered molybdenum was considerably less expensive than TZM, and a much more massive piece of material was required for the uninserted ejector impression block than for the cover impression block.

b. High-density, pressed and sintered molybdenum seemed to be more notch sensitive than TZM, so discretion directed that

it be employed in the die half which incorporated fewer small radii features. In the semi-production hemisphere die, the ejector die fit that description better than the cover die.

TZM was selected for the ejector die with some trepidation, inasmuch as it had delaminated early in the die materials evaluation performed by the Lamp Metals and Components Department. To produce a material with reasonably isotropic mechanical properties and, thereby, to minimize the risk of delamination, the supplier recommended fabricating the TZM cover die from an upset forged, forward extrusion. To explore the effect of forging practices further, the supplier was also requested to forge one of the two required hemispherical impressions into the plate.

The details of the conversion were as follows: A vacuum-arc melted ingot was extruded to a 7" diameter forging billet and recrystallized. The forging blank cut from that billet was heated to 2300°F and reduced to 75% of its original height by free upset forging. (Repeated blows and several reheatings were required.) Efforts to hot punch a 2" hemispherical impression into the upset forged plate were unsuccessful, due to the gross deformation of the warmed, hot work steel punch. The room temperature hardness of the TZM forging was 257 to 267 BHN. After stress relief annealing the forging for one hour at 2350°F, the hardness was reduced to a level of 246 to 253 BHN.

Photographs of the completed die are presented as Figures 156 and 157. Because the supplier was unable to produce a TZM forging to the required dimensions, high-density, pressed and sintered molybdenum pieces were inserted in the four corners of the A plate opening that were not filled by the circular forging. The necessity of those inserts is questionable.

3. OPERATIONAL RESULTS

After fabrication of the pilot production hemisphere die was completed, the refractory metal impression blocks were stress relief annealed, simply as a precautionary measure, even though the die materials evaluation performed by the Lamp Metals and Components Department had failed to reveal any advantage for that procedure. The TZM cover impression block was annealed for 1/2 hour at 1150°C in a hydrogen atmosphere, as recommended by the supplier. The high-density, pressed and sintered molybdenum ejector impression block was annealed for one hour at 875°C in a hydrogen atmosphere.

After having installed the pilot production hemisphere die in their die casting machine, but before making a single shot, Dort coated the die with a paste-like mixture of Type Z Molykote and petroleum oil. That mixture was "baked on" at 600°F to provide protection against soldering.

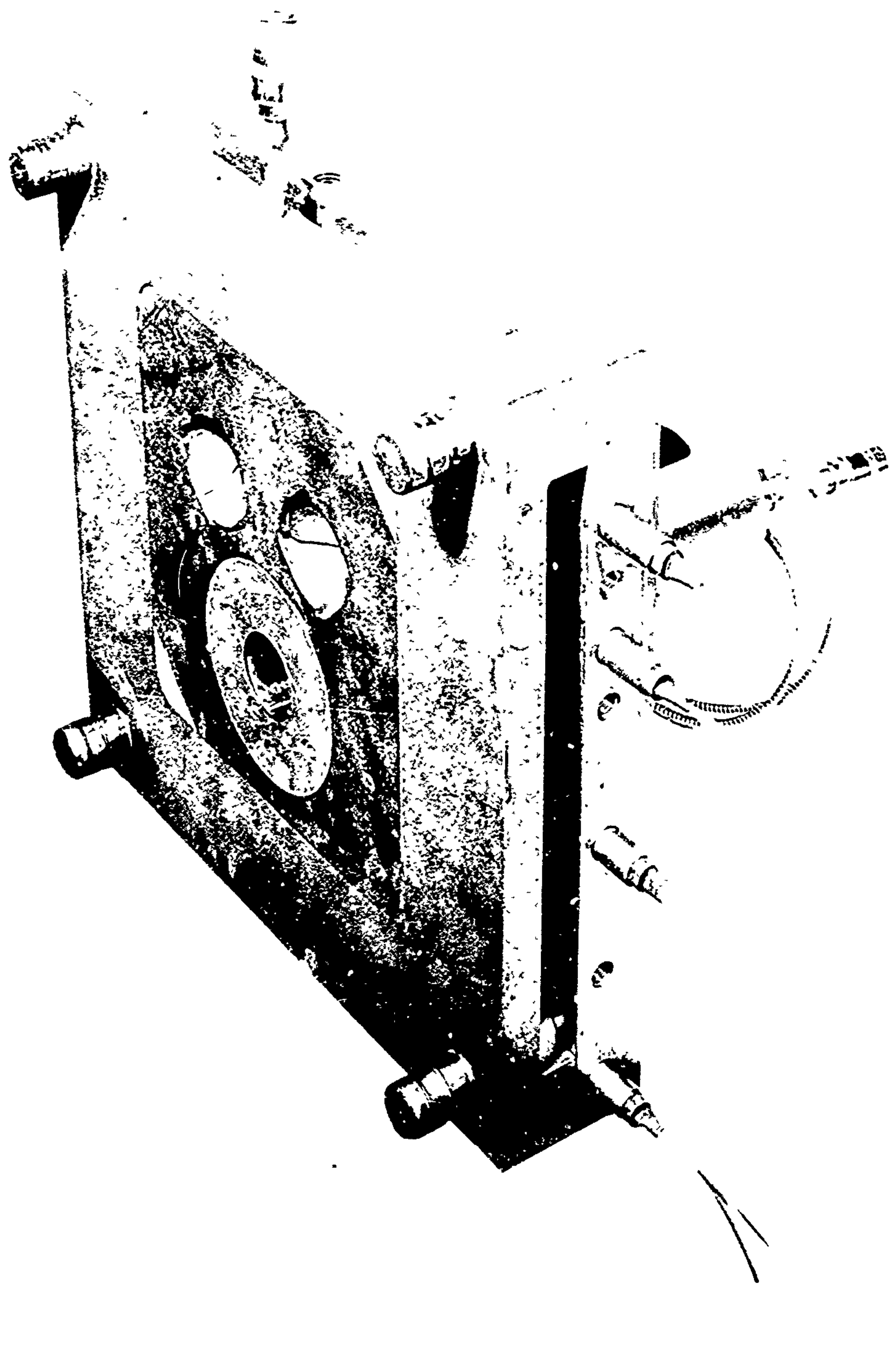


Figure 156: Photograph of the cover half of the completed pilot production hemisphere die and the associated pressure injection system.

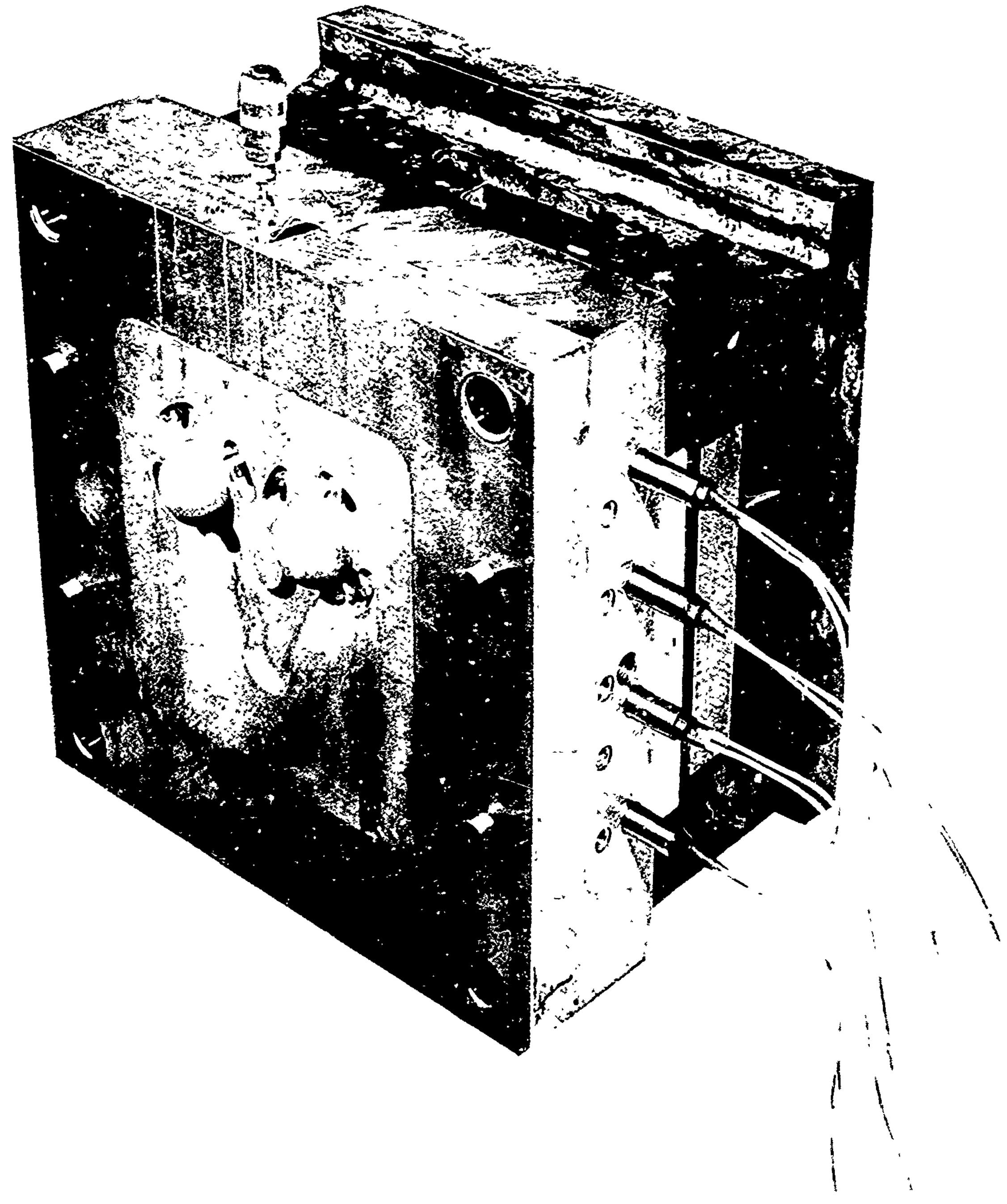


Figure 157: Photograph of the ejector half of the completed pilot production hemisphere die.

Based on their exploratory work, Doehler Jarvis had recommended the following operating conditions for the pilot production of die cast malleable iron hemispheres:

Metal Temperature	2700°F
Gate Velocity	56 ips
Pressure on Metal	4000 psi
Die Temperature	600°F
Dwell Time	3.8 seconds

Dort discovered that a dwell time of 3.8 seconds inevitably permitted the malleable iron biscuits to explode. Four seconds was found to be the minimum dwell time that could be employed without encountering biscuit explosion on the 400 ton Lester horizontal die casting machine with a heated, composite shot sleeve. Having established the minimum dwell time (which, by definition, was also the optimum dwell time), Dort proceeded to optimize the injection pressure and the injection velocity. The operating parameters finally selected are listed below:

Metal Temperature	2700°F
Gate Velocity	50 ips
Pressure on Metal	2850 psi
Die Temperature	600°F
Dwell Time	4 seconds

A coating of acetylene black was applied to the face of the die and the bore of the shot sleeve prior to each shot. The beryllium-copper plunger tip, which was fitted into the composite René 41, beryllium-copper, stainless steel shot sleeve, with a clearance of less than 0.001", was lubricated with LaFrance KR-3 plunger lubricant after every second shot. The injection system performed flawlessly, without resort to protective devices, such as asbestos paper liners.

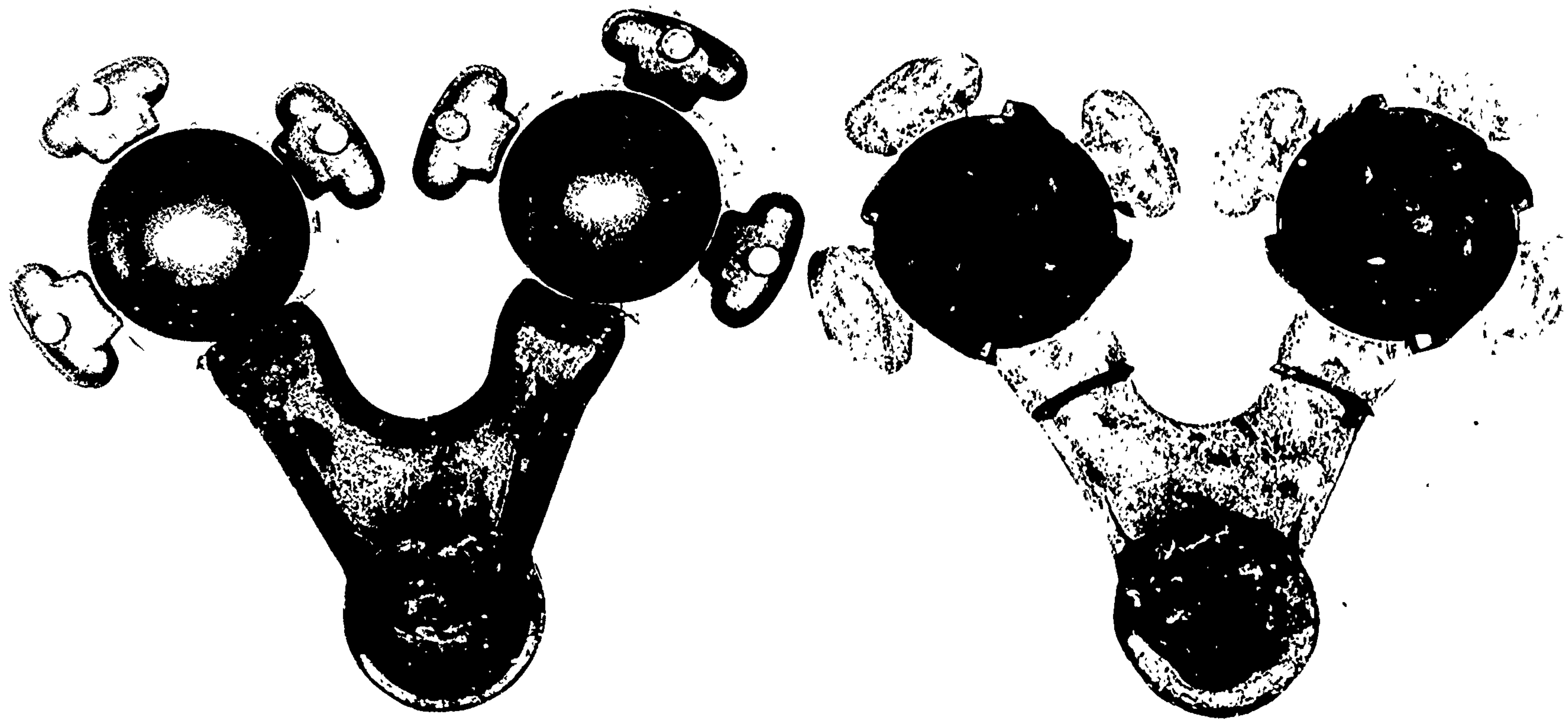
The castings which were produced exhibited excellent surface finishes, as may be seen in Figure 158, but they also suffered from three prominent defects:

a. Longitudinal cracks on the inside of the hemisphere, apparently tracing the radial projection of the flute centerlines

b. Longitudinal cracks on the outside of the hemisphere following the intersection of the high side of the flute and the hemisphere

c. Hot tears in the in-gate.

Examples of the internal and external cracks may be seen in Figures 159 and 160, respectively.



Ejector Face View

Cover Face View

Figure 158: Malleable iron gate die cast in the hemisphere die early in the pilot production run.

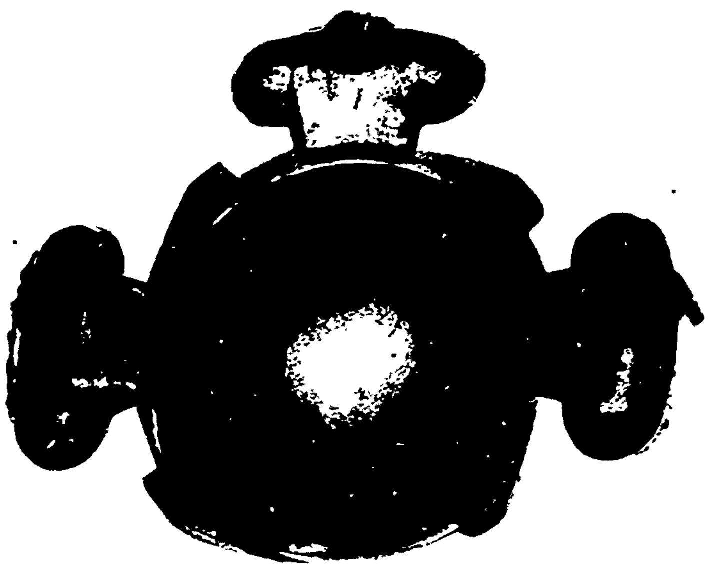


Figure 159: Example of the internal longitudinal cracks observed in the die cast, pilot production malleable iron hemispheres.

Figure 160: Example of the external, longitudinal (and circumferential) cracks observed in the early die cast, pilot production malleable iron hemispheres.

The hot tears in the in-gate were attributed to the tensile stresses created between the castings and the biscuit by the shrinkage that occurred during solidification and cooling. The external, longitudinal cracks were attributed to either shrinkage of the casting on the core or to the concentration of stresses created by the disparity of cooling rates between the flutes and the unfluted wall of the hemispheres. The internal cracks, however, clearly had their origin in the solidification pattern of the part.

The flutes, being the part of the casting having the greatest cross section, were the last part of the casting to solidify. When the flutes solidified, they were surrounded by the rigid skeleton of the previously solidified part of the casting. The tensile stresses created by the solidification shrinkage of these isolated molten pools, therefore, could only be accommodated by plastic deformation (and, in some of the hemispheres, at least a portion of the inner surface overlying the centerline of the flutes was drawn in, forming a marked depression). In general, however, as-cast malleable iron does not have sufficient ductility to permit deformation to occur. Therefore, it fails, by forming longitudinal cracks on the inner surface of the hemispheres, along the radial projections of the flute centerlines. (The same mechanism may have also been active in the formation of the external longitudinal cracks.)

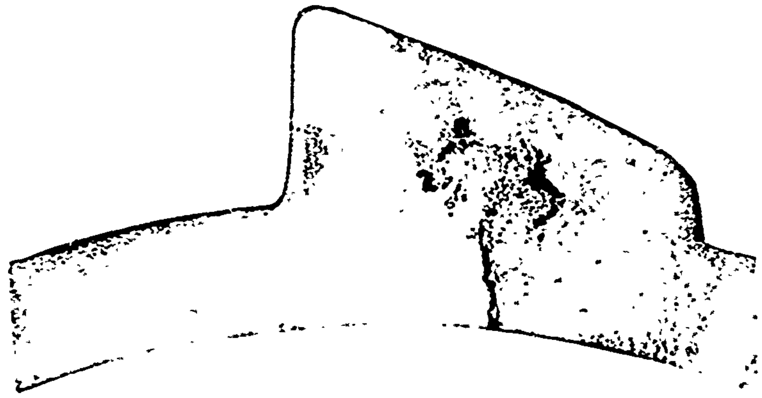
A number of hemispheres were sectioned parallel to their bases to establish the relationship between the internal, longitudinal cracks and shrinkage porosity. Photomacrographs of several of those sections are displayed in Figure 161.

In an effort to eliminate the hot tearing problems, Dort undertook an abbreviated program to identify a cast iron composition with superior resistance to hot tearing. Dort's exploration included an investigation of the effect of the carbon equivalent, the carbon-to-silicon ratio, and additions of molybdenum, copper, and nickel. The effects of die temperature and pouring temperature were also investigated.

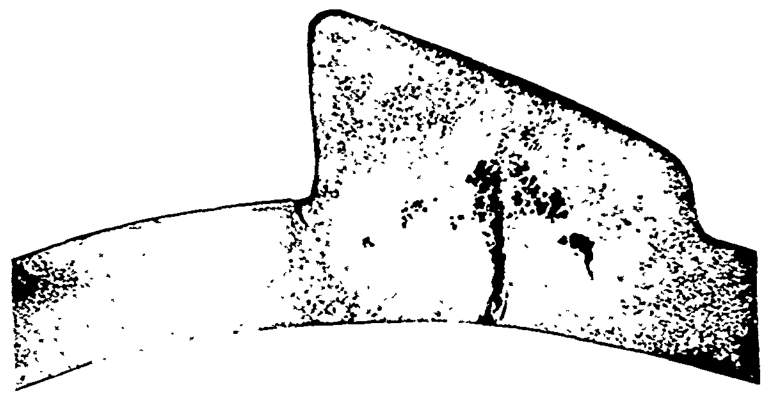
As a result of their investigation, Dort was able to affect a great improvement in the quality of the castings, but they were unable to completely eliminate the longitudinal cracks found in the inner surface of the die cast malleable iron hemispheres. The low levels of alloying additions required to affect that improvement, however, did impose a significant burden of cost on the process and the product, as will be seen in Section IX.

Had additional time and funding been available, there are at least three design alternatives that might also have been explored.

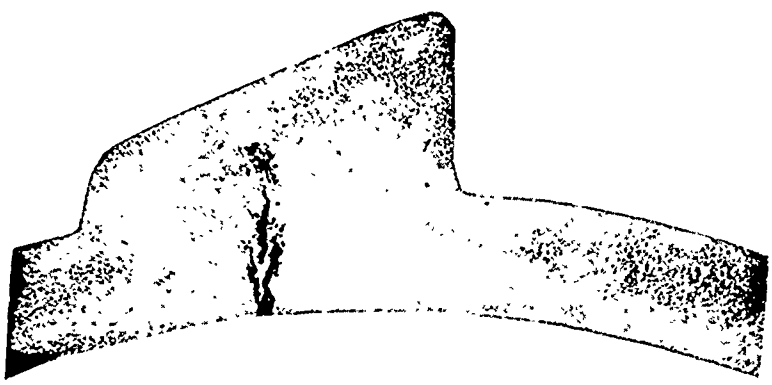
a. Additional liquid metal might have been supplied to the flutes during solidification to compensate for the solidification shrinkage concentrated in that area. This could have been



1/4" crack length Mag = 4X



5/16" crack length Mag = 4X



3/8" crack length Mag = 4X

Figure 161: Sectional view of the shrinkage porosity found in the flutes of the die cast malleable iron hemispheres and the hot tears associated with it.

accomplished by using either the injection pressure, or gravity, or both. In-gates of unconventionally large cross sections might have fed metal directly from the runner into one or more of the flutes. (Explosions indicate that metal remains molten in the biscuit and areas of large cross section for considerable periods of time.) To make use of gravity, overflows having a cross section equal to the heaviest flute section might have been connected directly to the flutes without gates and then carried upward with an ever-increasing cross section analogous to the risers employed in static casting. (This approach can be most easily employed on a horizontal die casting machine. Heavy gates and overflows are conventionally avoided in die casting, because they increase trimming problems and reduce the metal yield.)

b. By causing the fluted and unfluted areas of the hemispheres to solidify simultaneously, the concentration of shrinkage porosity in the flutes might have been avoided. (They were solidifying only after the unfluted wall was completely frozen.) To accomplish that end, the cross-sectional area of the flutes could have been reduced by adding a raised feature on the hemispherical core that would have formed a depression on the inner surface of the casting directly beneath the flute. (A good analogy is the grooves cast into the prototype hemisphere--refer to Figure 146.) Inasmuch as each flute lies in a plane oblique to the direction of ejection, however, a simple groove or depression would have interfered with ejection. The feature would had to have been carefully designed with ejection in mind. The effective cross section would have been defined, following the foundryman's practice, as the diameter of the largest circle that could be inscribed in any given section.

c. Bracketing, another foundryman's technique, could have strengthened the casting in the areas in which hot tearing was occurring. (A bracket is a raised feature on the part which solidifies before the rest of the part and attains sufficient strength to resist the stresses generated by solidification shrinkage.) The bracket would also had to have been carefully designed so as not to interfere with ejection. With the hemisphere in the base-up position, the brackets might have taken on the appearance of steps descending into a bowl. (One difficulty with this approach is that it would have increased the weight of the hemispheres somewhat.)

After three days of casting, Dort noted that the hemispherical cores on the high-density, pressed and sintered molybdenum ejector impression block appeared to be suffering pitting or erosion. Wear lands were also observed to be forming at the base of the cores. Figure 162 illustrates the extent of those problems.

<u>Figure 162</u>: Wear and erosion of the high-density, pressed and sintered molybdenum ejector impression block from the pilot production hemisphere die after three days of producing malleable iron castings.

Dort attributed the formation of the wear lands to the as-cast hardness of the malleable iron, e.g., 52.5 R_C, in the flute area. It was felt that a softer, more ductile material might relieve both the wear problem and the hot tearing problem. Therefore, with the concurrence of the prime contractor and the project engineer, Dort proceeded to die cast 1020 steel (MIL-S-22146-1C-1020). However, the steel, too, was found to be subject to hot tearing. It was more expensive than the malleable iron, and controlling its chemical analysis was found to be a much more difficult problem than that posed by the malleable iron (for which a thermal arrest type of carbon equivalent tester could be used). The higher melt temperature made the steel generally more difficult to handle than the malleable iron and also caused grave concern over die life.

Dort then pursued a second round of experiments with malleable iron compositions, which again reduced, but did not eliminate, the hot tearing. As a result of those experiments, Dort concluded that the malleable iron composition most resistant to hot tearing was an alloyed iron with a carbon equivalent of 3.2% to 3.3%. The optimum pouring temperature for that material was found to be 2380°F to 2410°F, with the other operating parameters maintained at the values indicated above.

Following the round of experiments with malleable ion, Dort made one last attempt to eliminate the problem of hot tearing by substituting a low alloy steel for the malleable iron. The alloy selected was AISI 4620, which was, by far, the most expensive raw material evaluated. In the course of the experiment, Dort unexpectedly encountered welding in the René 41 shot sleeve liner. The welding was unexpected, because none had been experienced in the heavy-walled René 41 shot sleeve liner used in the cooperative program with the Research and Development Center. The obvious conclusion was that the interface between the René 41 liner and the beryllium-copper heat transfer medium offered so much resistance to heat transfer that the liner with a 1/8" wall had experienced higher temperatures than the liner with the 1/4" wall, simply as a result of its lower heat capacity.

The die cast AISI 4620 hemisphere seemed to be no less prone to hot tearing than the malleable iron hemispheres; therefore, Dort recommended proceeding with the pilot production, using the best known malleable iron practice.

Inasmuch as Dort had been unable to eliminate the hot tearing completely, standards for acceptability had to be established. In establishing those standards, several factors were considered.

a. The precision-cast prototypes, which had performed satisfactorily, had also exhibited hot tearing. (Refer to Figures 145, 146, and 147.)

b. No correlation could be established between the length of the cracks and the degree of porosity revealed by sectioning the hemispheres. (See Figure 161.)

c. The cracks invariably terminated at the pipe which formed along the centerline of the flutes. Therefore, the crack depth never exceeded 2/3 of the combined thickness of the wall and the flute at any given point. (See Figure 161.)

To define reasonable expectations for the process and the part, Dort compiled a record of the length of the longitudinal cracks observed on the inner surface of the hemispheres produced during their alloy improvement efforts. The data are tabulated in Table LI. The results of Runs 7, 17, 21, 22, 23A, and 24 indicated that a maximum crack length of 1/2" would be an acceptable standard. The Air Force Project Engineer agreed.

A total of 420 shots were produced by Dort in their material improvement program. During that period, the initially rapid development of wear lands at the bases of the hemispherical cores in high-density, pressed and sintered molybdenum ejector impression blocks slowed to an imperceptible rate, presumably because the force of the shrinking castings was being spread over an increasingly large area.

At the conclusion of the materials improvement effort, Dort fixed the metal analysis and the operating parameters.

The alloyed malleable iron, with a carbon equivalent of 3.2% to 3.3%, was selected by Dort for the pilot production demonstration. The operating conditions employed were:

Metal Temperature	2400°F
Gate Velocity	50 ips
Pressure on Metal	2850 psi
Die Temperature	600°F
Dwell Time	4 seconds

After having made 402 additional shots, or a cumulative total of 822 shots, a new problem was encountered. Nearly every casting produced on the operator's side of the die was found to be cracked circumferentially on the inner surface, 180° from the in-gate. The problem is illustrated by Figure 163. In consultation with Doehler Jarvis, the only reason that could be suggested for the problem was pitting or roughening of the surface of the hemispherical core. A close examination of the core which was producing the defective castings indicated that the hemispherical surface of that core was rough, indeed, as may be seen in Figure 164. One troubling aspect of this discovery is that in the die materials evaluation conducted by the Lamp Metals and Components Department, high-density, pressed and sintered molybdenum was not particularly prone to erosion and pitting. It was, in fact, notably superior to TZM in that respect. This may indicate that the gating and/or the geometry of the hemisphere aggravated the problem.

Table LI--Summary of the Lengths of the Longitudinal Cracks Observed on the Inner Surfaces of the Hemispheres Die Cast During Dort's Material Improvement Effort

Run Number	Percentage of the Castings Having a Maximum Crack Length Equal To or Less Than the Column Heading but Greater Than the Column Heading to the Left										Cumulative Percentage of the Castings Having a Crack Length Less Than or Equal To the Column Heading		
	0	1/16"	1/8"	3/16"	1/4"	5/16"	3/8"	7/16"	1/2"	Over 1/2"	1/4"	3/8"	1/2"
1	--	--	--	--	--	--	--	--	--	100.0	0	0	0
2	--	--	--	--	--	--	--	--	--	100.0	0	0	0
3	--	--	--	--	--	--	--	--	--	100.0	0	0	0
4	--	--	--	--	--	--	--	--	--	100.0	0	0	0
5	--	--	--	--	--	--	--	--	--	100.0	0	0	0
6	--	--	--	--	--	--	--	--	--	100.0	0	0	0
7	3.7	--	7.4	7.4	33.3	25.9	3.7	11.1	3.7	3.7	51.8	81.4	96.3
8	--	--	--	--	--	--	11.1	--	--	88.9	0	11.1	11.1
9	10.0	5.0	20.0	5.0	15.0	10.0	10.0	5.0	--	20.0	55.0	75.0	80.0
10	--	--	3.8	--	7.7	3.8	7.7	11.5	3.8	61.5	11.5	23.0	38.5
11	--	--	--	--	--	--	--	--	--	100.0	0	0	0
12	--	--	--	--	--	--	--	2.9	--	97.1	0	0	2.9
13	20.0	--	--	--	10.0	--	--	--	10.0	60.0	30.0	30.0	40.0
14	--	3.3	3.3	26.7	10.0	26.7	3.3	3.3	--	23.3	43.3	73.3	76.7
15	20.0	5.0	5.0	--	15.0	10.0	10.0	10.0	2.0	25.0	45.0	65.0	75.0
16	--	--	--	--	--	--	--	--	--	100.0	0	0	0
17	--	--	--	--	28.6	21.4	7.1	21.4	7.1	7.1	28.6	57.1	92.9
18	--	--	5.5	16.7	22.2	16.7	5.5	--	5.5	27.8	44.4	66.6	72.2
19	4.5	4.5	9.1	--	4.5	13.6	22.5	--	4.5	36.4	22.6	58.7	63.6
20	16.7	8.3	--	8.3	16.7	16.7	--	8.3	8.3	16.7	50.0	66.7	83.3
21	--	--	5.0	10.0	15.0	45.0	10.0	5.0	--	10.0	30.0	85.0	90.0
22	--	--	--	13.6	4.5	18.2	31.8	18.2	9.1	4.5	18.1	68.1	95.6
23A	20.0	--	5.7	11.4	22.8	14.3	2.9	14.3	2.9	5.7	59.9	77.1	94.3
23B	18.0	13.3	13.3	11.7	1.6	8.3	8.3	3.2	1.6	20.0	58.2	74.8	80.0

Table LI--Summary of the Lengths of the Longitudinal Cracks Observed on the Inner Surfaces of the Hemispheres Die Cast During Dort's Material Improvement Effort
(Continued)

Run Number	Percentage of the Castings Having a Maximum Crack Length Equal To or Less Than the Column Heading but Greater Than the Column Heading to the Left										Cumulative Percentage of the Castings Having a Crack Length Less Than or Equal To the Column Heading		
	0	1/16"	1/8"	3/16"	1/4"	5/16"	3/8"	7/16"	1/2"	Over 1/2"	1/4"	3/8"	1/2"
24	50.0	--	--	11.1	11.1	5.5	16.7	5.5	--	--	72.2	94.4	100.0
25	--	--	--	--	3.8	3.8	15.4	11.5	19.2	41.2	3.8	23.0	58.0
26	--	--	20.0	10.0	--	30.0	--	--	--	40.0	30.0	60.0	60.0
27	--	--	--	--	25.0	--	16.7	--	8.3	50.0	25.0	41.7	50.0
28	--	--	--	2.5	--	7.5	5.0	5.0	--	80.0	2.5	15.0	20.0
1020 Scrap 29 1020 Ingot	23.5	--	--	--	2.9	5.9	8.8	5.9	5.9	47.1	26.4	37.3	52.9

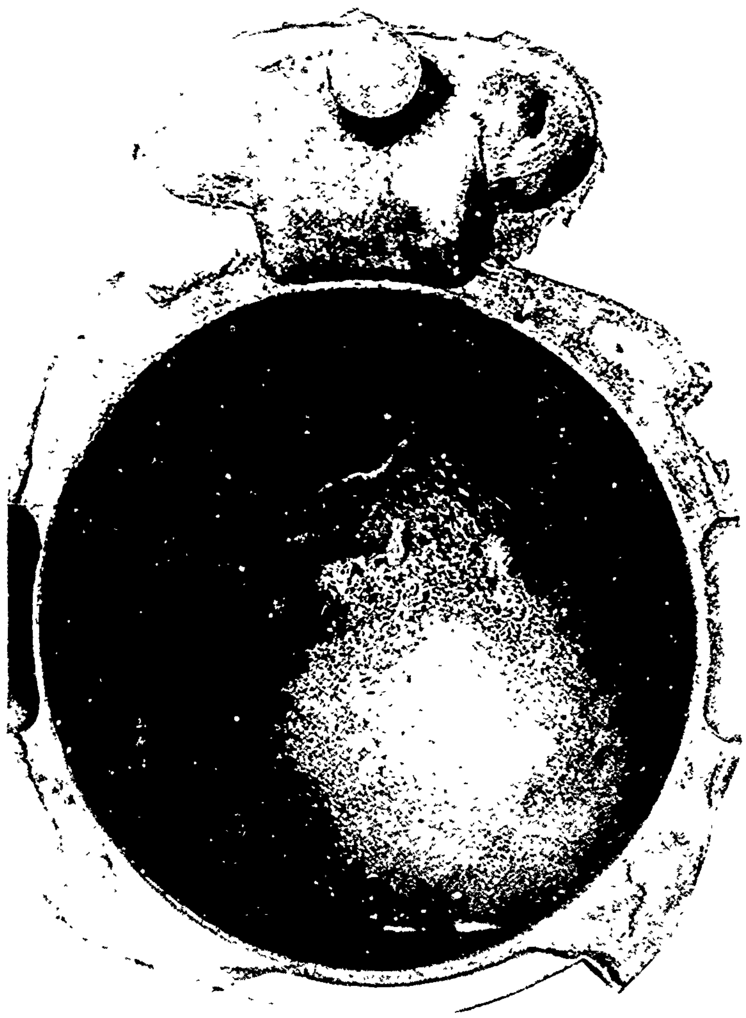


Figure 163: Circumferential cracks discovered on the inner surface of the malleable iron, pilot production castings after 822 shots.

Figure 164: Condition of the operator-side, hemispherical, high-density, pressed and sintered molybdenum core after 822 shots.

Dort's solution to the problem was to remove the die to the mold maker, where the flatness and parallelism of the impression block was checked; the ejector pins and ejector pin holes were checked; and the concentricity and draft at the base of the hemispherical cores was checked. No indication of warpage or distortion was discovered, not even in the sleeve area. The ejector pins were satisfactory, and the ejector pin holes were worn only in the length ahead of the retracted pins. The results of the inspection of the cores are presented in Table LII.

After inspecting the die, the die maker made a "kiss cut" at the base of each of the hemispherical cores to produce a 5° draft angle. The small pits were polished out of the cores, the cavity of the ejector die was polished, and the runners were cleaned up.

The die was returned to service. The measures taken to eliminate the circumferential cracking had been successful. Completing the pilot production phase of the contract, 1936 additional shots, or a cumulative total of 2761 shots, were made on the die.

One of the as-cast hemispheres from the pilot production run was sectioned. The microstructures that had developed in the fluted and unfluted sections displayed a consistent difference in the ratio of the major microconstituents. That difference can be observed in Figures 165 and 166. Diamond pyramid impressions were made in the fluted and unfluted areas at a load of one kilogram. The microhardness measurements were made with a 50X objective. For the fluted area, the values ranged from 628 to 675 DPH, with an average value of 656 DPH. For the unfluted area, the values ranged from 545 to 792 DPH. The great variance in these values suggests that little significance should be attached to the average hardness value of 637 DPH, which would appear to indicate that a higher hardness had been attained in the thicker, fluted section than in the thinner, unfluted section.

All of the castings which met the acceptability standard were annealed. The Lamp Metals and Components Department sectioned one annealed hemisphere, selected at random from the pilot production castings. That casting was then prepared for microscopic examination and the determination of microhardness. The annealing treatment to which the hemispheres were all subjected was the standard batch anneal used by the Moline Malleable Iron Company to ferritize their castings:

a. Heat to 1620°F and hold for 40 hours

b. Cool rapidly to 1450°F

c. Cool at 10°F per hour from 1450°F to 1250°F

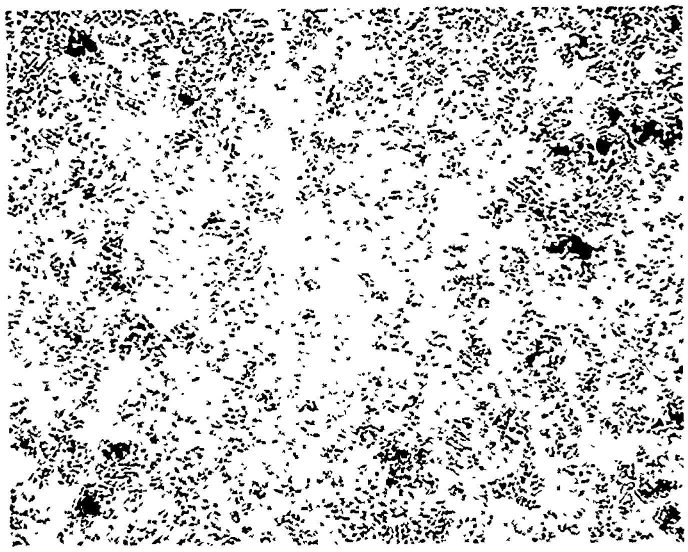
d. Cool to 1180°F and hold for six hours

e. Air cool to room temperature.

Table LII--Results of the Dimensional Inspection of the Hemispherical Cores of the Pilot Production Die After 822 Shots

	Operator Side		Helper Side	
Location*	Indicator Runout (in)	Draft (in)	Indicator Runout (in)	Draft (in)
0	.000	.009	.0000	.008
22-1/2	+.008	.008	--	--
45	+.005	.005	.0000	.001
67-1/2	+.004	.008	--	--
90	+.008	012	.0015	.012
112-1/2	+.002	.005	--	--
135	+.008	.003	-.0005	.001
157-1/2	+.009	.003	--	--
180	+.008	.005	-.0015	.011
202-1/2	+.003	.001	--	--
225	+.002	.004	.0000	.002
247-1/2	.000	.004	--	--
270	.000	.008	+.003	.010
292-1/2	-.005	.003	--	--
315	.000	.001	-.001	.004
337-1/2	-.003	.010	--	--

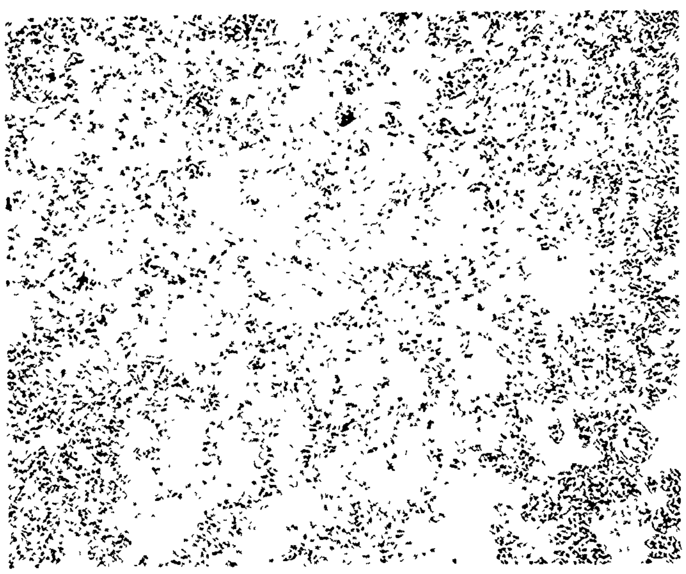
*Degrees from center of in-gate, measured clockwise.



Etchant: Nital Mag. = 200X

Fluted Section

Hardness Range = 628 - 675 DPH
Avg. Hardness = 656 DPH

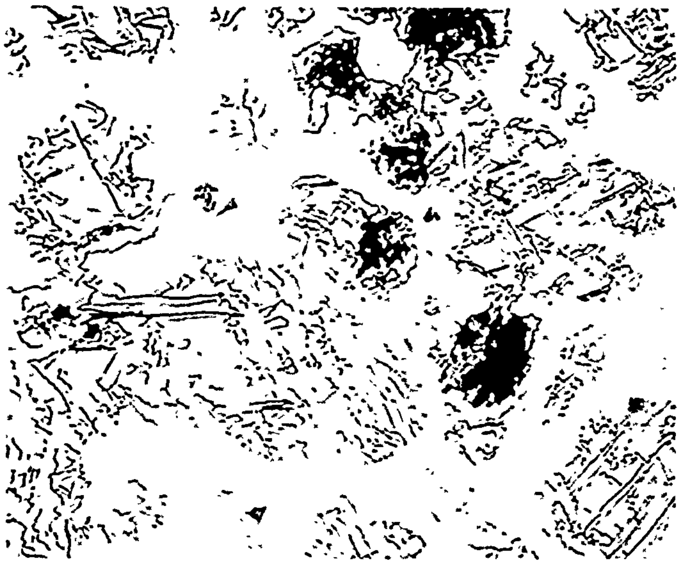


Etchant: Nital Mag. = 200X

Unfluted Section

Hardness Range = 545 - 792 DPH
Avg. Hardness = 637 DPH

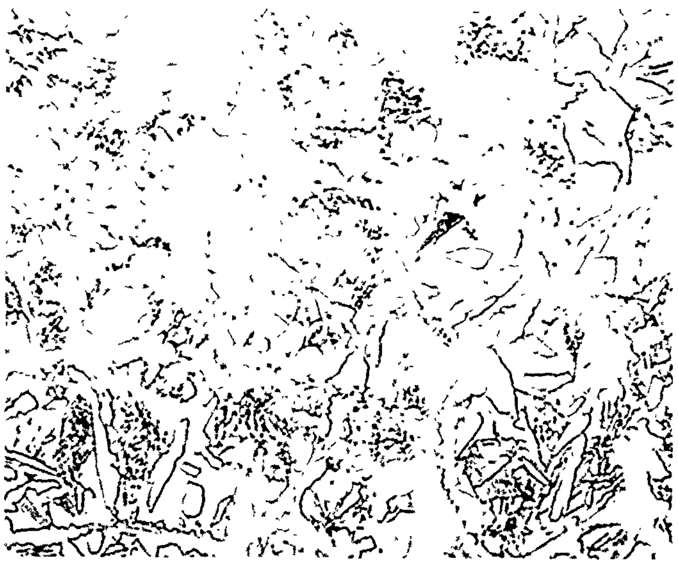
Figure 165: Microstructure of the alloyed 3.25% CE malleable iron hemispheres, as-die cast in pilot production.



Etchant: Nital Mag. = 1000X

Fluted Section

Hardness Range = 628 - 675 DPH
Avg. Hardness = 656 DPH



Etchant: Nital Mag. = 1000X

Unfluted Section

Hardness Range = 545 - 792 DPH
Avg. Hardness = 637 DPH

Figure 166: Microstructure of the alloyed, 3.25% CE malleable iron hemispheres, as-die cast in pilot production.

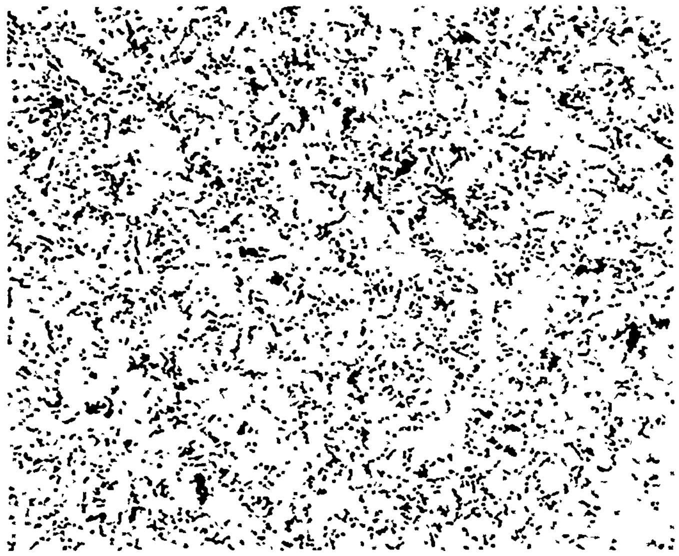
The microstructures that were developed by that heat treatment proved to be one of the greatest surprises of the entire project. Contrary to expectations, both the grain size and the size of the graphite nodules were significantly larger in the unfluted (thinner) section than in the fluted (heavier) section. This phenomenon can be observed in Figures 167 and 168.

One of many possible explanations for that paradox is that the rapid solidification and cooling rate that prevailed in the thinner, unfluted section promoted the retention of an extremely unstable carbide, which decomposed to form preferred nuclei when the castings were reheated to the annealing temperature. The widespread appearance of a light-etching, discreet phase in the microstructure of the as-cast, unfluted section at 1000X might be construed as substantiation of the proposed mechanism, inasmuch as that phase does not appear in the fluted section. (See Figure 166.) No funds were authorized under this contract for quantitative metallography or phase identification, however, so no attempt was made to verify the proposed explanation.

Microhardness readings, clustered around the shrinkage porosity in two fluted sections from a single, annealed, pilot production hemisphere, were in very good agreement. The range of values was identical for each section, i.e., 286 to 300 DPH. One section yielded an average hardness value of 294 DPH. The other yielded a value of 295. The combined average was 294.5 DPH.

The range of microhardness values measured along the centerline of the thinner, unfluted section of the annealed hemisphere was again found to be broader than the range observed in the fluted section, paralleling the experience with the as-cast hemisphere. (The range of values was 251 to 304 DPH.) The average value (281 DPH) was again less than the comparable value for the fluted section of the annealed hemisphere.

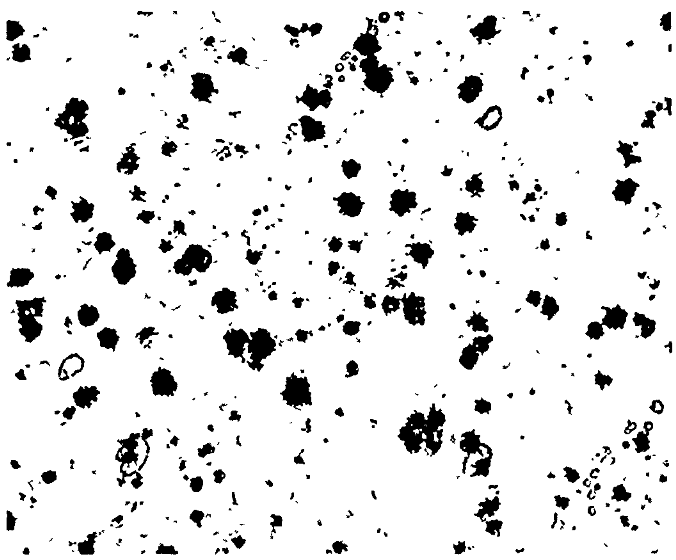
Adding to the confusion, a series of microhardness readings was made close to the edge of the unfluted section. They exhibited an equally broad range (255 to 309 DPH). The average hardness value along the edge of the unfluted section was 293 DPH, higher than the average value along the centerline of the unfluted section; but, perhaps fortuitously, almost identical to the value determined for the fluted sections. Although no evidence of it was observed in the microstructure of the casting in question, it is quite common for the surfaces of batch annealed malleable iron castings to be decarburized. Paradoxically, if decarburization has occurred, the normal annealing treatment may yield microstructures having significantly higher hardnesses than ferritic malleable iron. To understand this phenomenon, we must briefly examine the mechanism of malleablization.



Etchant: Nital Mag. = 200X

Fluted Section

Hardness Range = 286 - 300 DPH
Avg. Hardness = 295 DPH

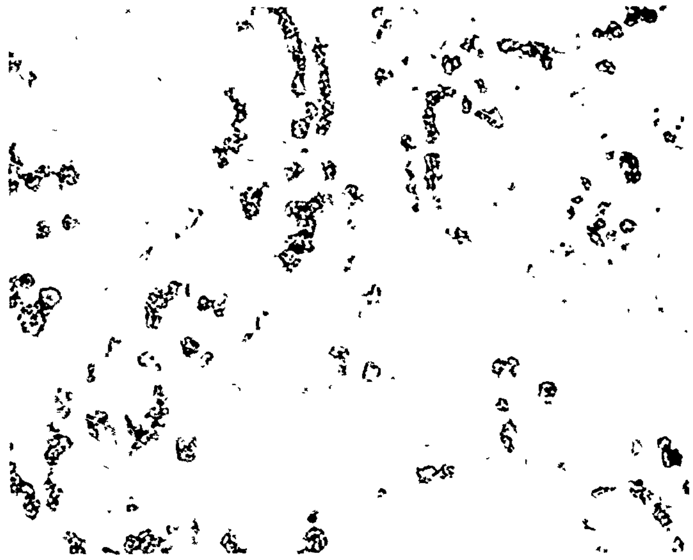


Etchant: Nital Mag. = 200X

Unfluted Section

Hardness Range = 251 - 304 SPH
Avg. Hardness = 281 DPH

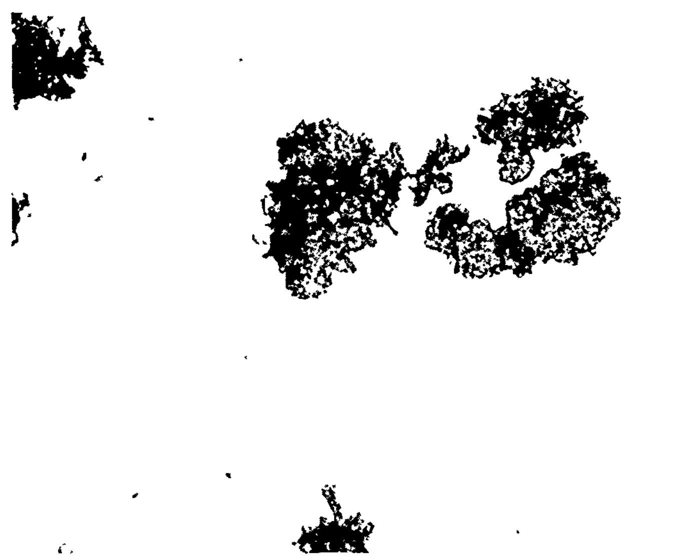
Figure 167: Microstructure of the alloyed 3.25% CE malleable iron pilot production hemispheres after annealing.



Etchant: Nital Mag. = 1000X

Fluted Section

Hardness Range = 268 - 300 DPH
Avg. Hardness = 295 DPH



Etchant: Nital Mag. = 1000X

Unfluted Section

Hardness Range = 251 - 304 DPH
Avg. Hardness = 281 DPH

Figure 168: Microstructure of the alloyed 3.25% CE malleable iron pilot production hemispheres after annealing.

Before annealing, virtually all of the carbon in malleable cast iron is chemically combined with iron and/or other metals as metastable carbides (neglecting the very small amount that remains in solid solution in the ferrite matrix). At the annealing temperature, those carbides decompose into carbon saturated austenite and graphite in the form of "temper carbon." (It should also be noted at this point that some alloying additions may form very stable carbides which do not decompose.) During slow cooling through the transformation range (e.g., 10°F from 1450°F to 1250°F), that carbon which had been in solid solution in the austenite is rejected and precipitates as graphite on the temper carbon nuclei, reducing the combined carbon to its solid solubility limit in ferrite (>.025%). However, if decarburization has reduced the carbon content of the malleable iron to less than its limit of solid solubility at the annealing temperature (e.g., 1.25% at 1620°F), no temper carbon nuclei will form. Slow cooling through the transformation range will then result in transformation to pearlite (a lamellar ferrite + Fe_3C eutectoid), which is significantly harder and stronger than the ferrite matrix of ferritic malleable iron.

One of the very important aspects of the die casting process is dimensional control. A representative sample of the die cast malleable iron hemispheres produced in the pilot production phase of the project was taken before and after annealing. Those castings were subjected to a complete dimensional inspection by the Lamp Metals and Components Department, but in the interest of brevity, the results for only three of the more critical dimensions are reported in Table LIII:

a. Flute diameter

b. Flange diameter

c. Height.

Perhaps the most significant observation that can be made about the results of the dimensional inspection is that the mean flange diameters and heights for the annealed castings fell within the allowable tolerances, i.e., 2.472 to 2.483 for the flange diameter and 1.250 to 1.258 for the height. (No meaningful comparison can be made for the flute diameter which is, in one sense of the word, fictitious, inasmuch as neither of the flute surfaces is, by design, truly concentric with the hemisphere.)

The fact that the mean values of the flange diameter and the height of the hemispheres, produced in the pilot casting operation, fell within the allowable tolerances is most surprising; however, when one realizes that the malleable iron castings produced in Dort's pilot production run expanded during annealing, whereas those castings on which

Table LIII--Results of the Dimensional Inspection of the Die Cast Malleable Iron Hemispheres From the Pilot Production Run

	Flute Diameter			Flange Diameter			Height		
Description	$\bar{X}$* (in)	σ** (in x 10^3)	N***	$\bar{X}$* (in)	σ** (in x 10^3)	N***	$\bar{X}$* (in)	σ** (in x 10^3)	N***
As-cast tops	2.7668	2.3	18	2.4499	2.4	18	1.2379	1.7	13
Annealed tops	2.7972	18.7	15	2.4795	7.0	15	1.2500	7.1	9
As-cast bottoms	2.7645	2.6	17	2.4507	2.8	18	1.2360	2.5	13
Annealed bottoms	2.7964	13.7	17	2.4782	15.4	17	1.2500	5.5	12

*$\bar{X}$ - Arithmetic mean

**σ - Standard deviation

***N - Number of determinations

the die design had been based shrank, as a result of annealing. Dort's annealing treatment reduced the flange diameter of the malleable iron hemispheres cast by Doehler 0.0007" per inch. The same annealing cycle increased the diameter of the pilot cast tops 0.012" per inch and increased the diameter of the pilot cast bottoms 0.011" per inch. Dort's annealing treatment reduced the height of the malleable iron hemispheres cast by Doehler 0.003" per inch but increased the height of the pilot cast tops 0.010" per inch and increased the height of the pilot cast bottoms 0.011" per inch.

Another extremely significant aspect of the data in Table LIII is the very great variance found in the dimensions of the annealed hemispheres (as indicated by the value of the standard deviation). Taking those variances into consideration, and assuming the dimensional measurements represented populations having normal distributions, the probability of the castings being in-tolerance was calculated. The results of that calculation were:

Part	Dimension	Percent Acceptable
Tops	Flange diameter	54.92
Tops	Height	37.08
Bottoms	Flange diameter	27.71
Bottoms	Height	42.65

Returning to the data in Table LIII, however, it is obvious that it was the annealing treatment which introduced the extreme variability; all of the dimensions exhibit a very modest variance as-cast, indicating that the casting process, per se, was under very close control. Although the source of the variability introduced by the annealing treatment was not demonstrated conclusively, visual inspection indicates that it was probably related to eccentricity in individual castings. If that proves to be a valid observation, improved annealing techniques should drastically reduce the variance observed in annealed, malleable iron hemispheres.

It should also be noted that there is excellent dimensional correspondence between the top and bottom hemispheres. This is a tribute to the mold maker's art.

After being annealed, the hemispheres were machined to the dimensions and configurations illustrated in Figure 155. A circular slot, 0.150" deep, was required in the flutes, just beyond the flange of the hemispheres, to accommodate a locking ring. The slotting operation required to produce

that slot necessitated an interrupted cut and proved to be most difficult. Apparently, not all of the difficulty was related to the interrupted nature of the cut, however. Steel tools, including a 16-tooth hole saw, proved to be inadequate, and a curved, single-point, sintered tungsten carbide tool had to be employed. Tool wear was excessive. The poor machinability reflected by those difficulties may indicate that the alloying elements added to reduce hot tearing also resulted in the retention of hard residual carbides in the microstructure of the annealed malleable iron hemispheres. Indeed, Figure 168 appears to reveal such residual carbides.

Another problem was also encountered during the machining operation. Many of the flute tabs left on the outside of the circular slot were found to be breaking off. The problem was ascribed to the great variance in flute diameter, as reported in Table LIII, and to the necessity of gripping the hemispheres by the flutes to perform the machining operation. As an expedient to reduce the scrap loss, a reduction in the slot depth was authorized. The long range solution, however, would appear to be twofold:

a. Reduce the distortion introduced by the annealing process

b. Produce the slots as a cast detail.

The second step, of course, would only be effective after having taken the first step.

After a representative sampling of the hemispheres had been machined, establishing a standard volume, they were weighed. The results are reported in Table LIV. There is little need for further comment. The tops were 3.5 to 4 grams heavier than the bottoms; the modification added 2.5 to 3 grams to the weight of each hemisphere. The variance is small, indicative again of good control of the casting process.

Figure 169 is a photograph of a number of the finished hemispheres.

Figures 170 and 171 are, respectively, photographs of the TZM cover impression block and the high-density, pressed and sintered molybdenum ejector impression block from the pilot production die after 2761 shots. One instructive exercise is to compare Figures 170 and 171 with Figures 117 and 118, which are photographs of the Lamp Metals and Components Department materials evaluation die after 3000 shots. It is obvious that TZM cover impression block from the pilot production die has resisted delamination better than the TZM insert in the materials evaluation die (cover half Insert 5). It is also obvious that

Table LIV--Statistical Analysis of the Weights of the Malleable Iron Hemispheres Die Cast in Pilot Production

	Original			Modified			Combined		
Description	$\overline{X}$ (grams)	σ (grams)	σ^2 (g^2)	$\overline{X}$ (grams)	σ (grams)	σ^2 (g^2)	$\overline{X}$ (grams)	σ (grams)	σ^2 (g^2)
Tops	137.72	1.558	2.427	140.72	1.538	2.367	140.13	1.543	2.379
Bottoms	134.10	1.458	2.125	136.84	0.5895	0.3475	136.30	0.8336	0.695
Spheres	--	--	--	--	--	--	276.43	1.240	1.537

Notes

$\overline{X}$ = Arithmetic mean

σ = Standard deviation

Original refers to castings made before the die was modified (406 good tops and 406 good bottoms)

Modified refers to castings made after the 5° taper was added to the cores (1673 good tops and 1673 good bottoms)

Combined refers to the effect of mixing the original and modified castings indiscriminantly. The combined values of $\overline{X}$ and σ were calculated:

$$\overline{X}_C = \frac{N_O \overline{X}_O + N_M \overline{X}_M}{N_O + N_M} \qquad = \frac{N_O \sigma_O^2 + N_M \sigma_M^2}{N_O + N_M}$$

Spheres refers to the weight of a pair of castings only (i.e., not including locking bands, etc.) The values of $\overline{X}$ and σ for the spheres were calculated:

$$\overline{X}_S = \overline{X}_{CT} + \overline{X}_{CB}; \quad \sigma_S^2 = \frac{N_{CT} \sigma_{CT}^2 + N_{CB} \sigma_{CB}^2}{N_{CT} + N_{CB}}$$

Figure 169: Photograph of a group of finished hemispheres die cast from malleable iron in pilot production.

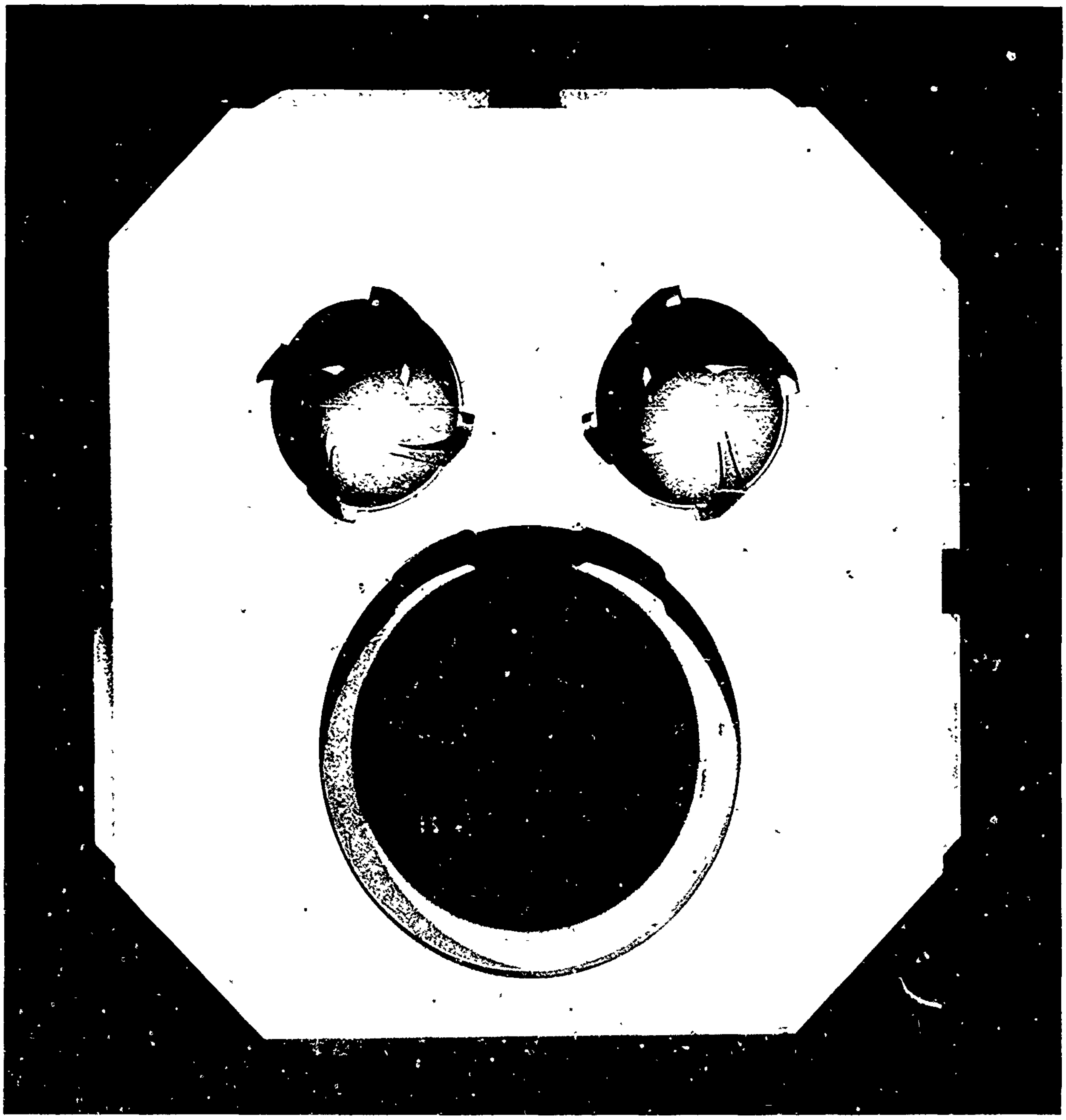


Figure 170: TZM cover impression block from the pilot production die after 2761 shots.

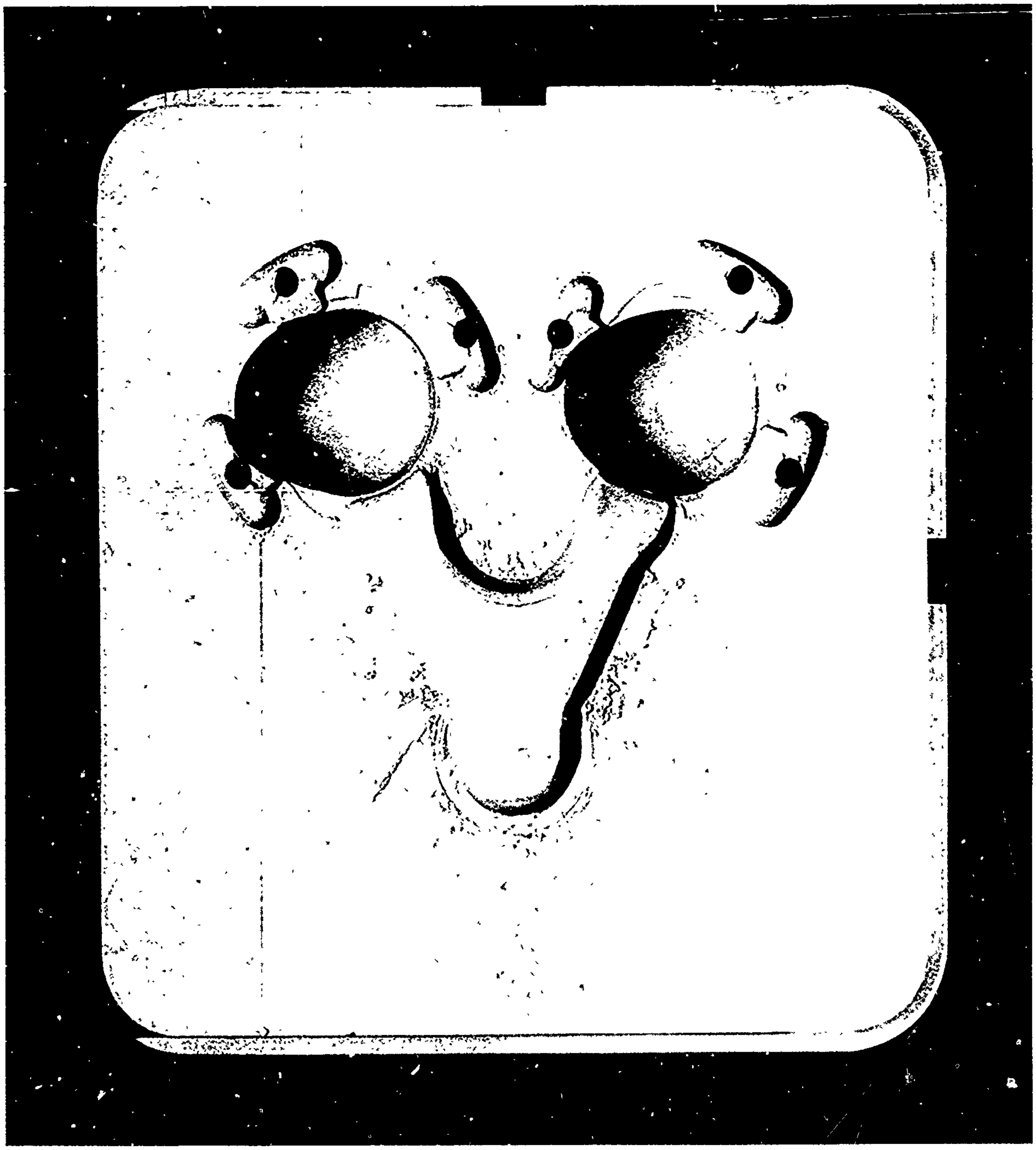


Figure 171: High-density, pressed and sintered molybdenum ejector impression block from the pilot production die after 2761 shots.

the high-density, pressed and sintered molybdenum ejector impression block has resisted gross cracking better than the high-density, pressed and sintered molybdenum ejector-half insert retainer plate from the materials evaluation die. The source of this improved performance was not pinpointed, but several possibilities can be suggested. Die design is one. The relatively large cross section of the runners in the pilot production die may have made the environment in that area less imposing than the environment in the runners of the materials evaluation die. The pilot casting was performed at an injection velocity of 50" per second. The maximum velocities experienced in the runners of the materials evaluation die occurred in the two branches immediately adjacent to the sprue (the point of minimum cross-sectional area). At those points, the gate velocities ranged from 48" to 68" per second during the course of each two-speed injection cycle. The malleable iron for the pilot production run was poured at 2380°F to 2410°F; the 304 stainless steel for the die materials evaluation was poured at 2732°F to 2912°F. The injection pressure for the pilot production was 2850 psi, versus 8400 psi for the die materials evaluation.

More specifically, the TZM cover impression block (Figure 170) was in excellent condition, although early warnings of eventual trouble were discovered. The TZM had been upset forged, "pancaked" to reduce the problem of delamination which it exhibited in the Lamp Metals and Components Department's die materials evaluation. The results were encouraging, although a hairline, circumferential crack, parallel to the face of the pancake, did appear in one cavity. That crack was found in the left-hand cavity (helper's side), about 11/16" below the parting face. One end of the crack began in an unfluted area of the cavity at 12 o'clock. It crossed the flute at 1 o'clock, ran into the flute at 4 o'clock, followed the groove which forms the high edge of the flute upward to the point at which the groove experiences a rapid change of slope and then ran vertically into the parting face. Although the crack cannot be seen in Figure 170, it was similar to the crack observed in the extruded molybdenum evaluated by Doehler Jarvis, but much finer. (See Figure 82.) Shrinkage of the castings in the die had also plastically deformed the TZM and raised very small lips at the point of intersection of the surfaces, forming the outer diameter of the hemispheres and the lower edges of the flutes. The sharp edges of the cavities above the in-gates were also very slightly rounded off, as a result of chipping or plastic deformation. None of these irregularities, however, had reached the point at which they were obvious.

The high-density, pressed and sintered molybdenum ejector impression block had not fared so well. The most striking evidence of deterioration was the large number of dents on the parting surface of the impression block. Dort reported that they had experienced no soldering whatsoever during the course of their campaign on the die, and they had handled the die with great care. Having eliminated soldering and the careless use of tools as possible sources of the denting, it had to be ascribed to particles of flash and/or lubricant which remained in the gap between the die halves during lockup. Indeed, the trail of small dents descending from the sprue is almost certainly related to excess lubricant and other debris which cascaded out of the bottom of the sprue immediately prior to lockup. Corresponding dents were also discovered on the shot sleeve flange, the TZM cover impression block, and the H-11 A and B plates directly below the sprue, although they were most pronounced in the relatively soft, high-density, pressed and sintered molybdenum.

A continuous dent surrounding the bottom of the sprue like a crescent moon was formed by the biscuit. The diameter of the shot sleeve liner was greater than that of the sprue. Consequently, as the biscuit solidified, the thickness of the cast sprue decreased, unloading the bottom of the sprue and permitting the full injection force to be borne by the relatively small area of that shoulder surrounding the sprue. Although the phenomenon is inconsequential, it might be avoided by making the diameter of the sprue the same as the shot sleeve.

Dents were also observed in the center of the sprue. Those dents originated with Dort's efforts to use the lock-up force of the machine to help extract a stuck plunger. When the pad protecting the impression block slipped, the sprue was dented by the steel bar that was being used as a tool.

Another obvious example of plastic deformation noted in the relatively soft, high-density, pressed and sintered molybdenum impression block may be seen in Figure 171, i.e., the formation of raised replicas of the flutes which were sunk in the TZM cover impression block. (No raised surfaces were observed in the TZM.)

Small cracks, which were assumed to presage the formation of gross cracking, issued from each ejector pin hole in the high-density, pressed and sintered molybdenum ejector impression block. A relatively long, thin crack, which also seemed to have most of the characteristics of a gross crack, was found at the point at which the inrushing metal was diverted into two streams. Its location suggests that it might have followed the course of the tip of the ball end mill that was used to form the edge of the runner. It might, therefore, delineate an area of high residual stress. Obviously, the crack also demarcates the planar surface of the bottom of the runner from the curved surface of the runner edge. Based on geometric considerations alone, that might be an area of thermal stress concentration.

A number of very fine cracks were also noted on the hemispherical cores which had most of the characteristics of heat checks. Some of these can be seen on the right-hand core (helper's side), at 4 o'clock, in Figure 171.

All of the irregularities in the high-density, pressed and sintered molybdenum ejector impression block after 2761 shots, which have been discussed up to this point, may be observed in Figure 171. There was, however, one other, very serious defect in that impression block which cannot be readily seen in the photograph. Directly above each in-gate and each overflow-gate, but nowhere else, a crack developed at the base of the hemispherical cores. At several gates, the crack was tightly closed; at others, it had begun to gape. In the latter areas, there was an indication that the shrinking castings were pulling "splinters" from the base of the core, which were parallel to the direction of ejection and ran from the crack at the base of the core to the hemispherical surface above.

Although the pilot production die remained in satisfactory condition for the continued production of castings, the problem which was developing at the base of the hemispherical cores in the high-density, pressed and sintered molybdenum would have required early repair. Techniques suitable for that task, however, are not part of the current state of the art; such repair could only have been performed on an experimental basis.

SECTION IX

COST STUDY

As indicated in Section I, the rationale for advancing the state of the art of ferrous die casting was to make accessible to the USAF a new metalworking technology with a dramatic potential for cost reduction. One of the objectives of the pilot production phase of the contract, therefore, had been the generation of cost data. The numbers generated are real and pertinent, but because the die design and the method and scale of the operation reflected compromises and economies, the numbers must be considered indicative only, and they are incomplete. Ultimate lives were not determined for the die and injection system, and a number of elements that contributed to the manufacturing overhead have been deleted, because their disclosure might have been detrimental to the competitive position of the Moline Malleable Iron Company.

What will be discussed here is shop cost; and, for the purposes of this discussion, shop cost will be defined as the sum of the direct material cost plus the direct labor cost plus the manufacturing overhead. Each of these elements will be discussed in turn, with reference to Dort's pilot production operation. Dort's shop costs will then be summarized, and opportunities for cost reduction in Dort's operation will be identified. Finally, shop costs for die cast malleable iron hemispheres will be projected, based on a mathematical model of a full-scale, production, die casting operation.

Table LV should be considered first, however, because it bears on several of the topics to be discussed. It summarizes the casting performed by Dort in the pilot production hemisphere die.

1. DIRECT MATERIAL COST

For the purpose of this discussion, direct material cost will be defined to include the following:

a. Cost of the injection system

b. Cost of the die

c. Cost of the metal cast

e. Cost of the utilities used

f. Cost of the supplies consumed.

Table LV--Summary of the Casting Performed by Dort in the Pilot Production Hemisphere Die

Lot No.	No. of Castings Produced (Hemispheres) Good Pcs	Scrap Pcs	Total	Remarks
Alloy Improvement				
Lots 1-29	280	560	840	
Pilot Production				
1	336	130	466	After Run 2, the die was modified by adding a 5° taper at the base of each core.
2	196	142	338	
3	461	64	525	
4	490	64	554	
5	518	68	586	
6	856	166	1,022	
7	461	106	567	
8	559	65	624	
Total (before die modification)	812	832	1,644	
Total (Runs 3-8)	3,345	533	3,878	86.26% yield (13.74% scrap)
TOTAL	4,157	1,365	5,522	

To calculate the costs of the injection system, the $998 cost of a replacement René 41 liner was subtracted from the total cost of a composite, René 41, beryllium-copper, stainless steel shot sleeve ($2200), because the liner was assumed to have an ultimate life of only 10,000 shots, whereas the remainder of the sleeve was assumed to have an ultimate life of 100,000 shots. The 10,000 shot life of the liner was based on the observation that Dort's liner required regrinding after 2500 shots. It was assumed that the liner could only endure three regrinding operations, and a quotation of $95 was received for the regrinding operation. Dort also concluded that the beryllium-copper plungers would have to be replaced every 500 shots; a quotation of $48 each was received for beryllium-copper plungers. Finally, the total cost of the injection system per shot was divided by the yield to obtain the cost per good shot.

Although there is no ambiguity concerning the original die cost, there is only uncertainty relative to its period of useful production, its adaptability to repair and/or reworking, the cost of repair and/or reworking, the ultimate die life, and the replacement cost. (Perhaps only the refractory metal impression blocks and the ejector pins would need to be replaced.) For the purpose of this analysis, the following, rather arbitrary, assumptions were made:

a. The period of useful service would have been 10,000 shots

b. The die could have withstood repair only three times, at a cost of 10% of its total initial cost each time

c. Therefore, the ultimate life of the die would have been 40,000 shots

d. The cost of replacing the die would have been 80% of the total original cost. (This is based on scrap credit for the refractory metals, a reusable die base, and the survival of the jigs, fixtures, tooling, and experience required to replace the die.)

Inasmuch as these assumptions made the die cost a function of the length of the production run, a value of 2,000,000 shots was arbitrarily assumed (approximately three year's production, at an average rate of 100 per hour, 24 hours per day--49 replacement dies would be required). The original cost of the pilot production die is broken down in Table LVI. An appropriate adjustment for yield must also be applied to the die cost.

The first step in analyzing the cost of the cast metal was to determine the materials balance. The materials balance determined for the pilot casting operation performed under this contract is represented by Table LVII. To appreciate the information in Table LVII, two pieces of supplementary information are required:

a. The average shot weight was 2.15 pounds

b. The average weight of a pair of trimmed hemispheres was 0.68 pounds.

Table LVI--First Cost of the Pilot Production Die

Materials	
TZM (2-1/8" x 11-3/4" x 12" round-cornered square, 99.0 pounds)	$2,550
High-density, pressed and sintered molybdenum (3-7/16" x 11-1/4" x 12-1/8", 169 pounds)	1,687
Fabrication (and nonrefractory metal components)	6,075
TOTAL	$10,312

Table LVII--Materials Balance for the Pilot Casting Operation

Materials In

Description	Wt (lbs)
Malleable pig iron	85.00
No. 1 heavy steel scrap	5.50
50% Fe-Si	5.00
Fe-alloys	3.25
Ca-Si-Mn	1.25
TOTAL	100.00

Materials Out

Description		Wt (lbs)
Good castings*		23.26
Recoverable scrap		69.08
Scrap castings	3.70	
Gates	56.28	
Pigged Surplus	9.10	
Total	69.08	
Unrecoverable scrap		7.66
Oxidation losses	2.20	
Handling losses	5.46	
Total	7.66	
TOTAL		100.00

*Average shot weight = 2.15 pounds
Average weight of a pair of trimmed castings = 0.68 pounds

Using that information, it was possible to construct Table LVIII, which itemizes the metal costs for the good castings that can be produced from a 100 pound melt. Note that the ferro-silicon and ferro-alloy additions were made to affect resistance to hot tearing; the calcium-silicon-manganese was added as a deoxidant.

The cost of the utilities required by the pilot casting operation was based on the experience of one day in which Dort was assumed to have made 325 shots (their daily average for Runs 3 through 8). The breakdown of those costs appears in Table LIX. They, too, must be adjusted to take into account the 86.26% yield.

The consumption of supplies was recorded by Dort for the entire pilot production campaign (Runs 1 through 8), which produced 1938 good pairs of hemispheres. The cost of those supplies is broken down in Table LX. The application for two of the items may not be obvious. The refractory wash was used to coat both ladles and crucibles; the acetylene was used to soot the dies.

Table LXI summarizes the direct material cost experienced by Dort during the pilot production of malleable iron hemispheres.

2. DIRECT LABOR COST

For the purpose of this discussion, the following activities will be defined as direct labor: melting, casting, trimming, inspection, annealing, finishing, machining, chemical analysis, setting up and tearing down the machine, the die and the instrumentation, maintenance, and housekeeping. These activities include unit operations, peripheral activities, and service activities.

In the sense that the term is used here, the unit operations were those interdependent activities that were performed simultaneously with the die casting operation itself, by the same team of operators, i.e., melting, casting, trimming, and inspection. To analyze the time spent on such activities during the pilot casting operation, Dort produced an activity chart for a full day's operation. That chart, which is reproduced as Table LXII, also indicates the amount of time spent on such peripheral activities as routine preventative maintenance, preparation of the furnaces, injection system, and die and cleanup operations (although the cleanup operation might better have been a service activity). Using the activity chart as a basis, Table LXIII was prepared to indicate, in more detail than the summary in Table LXII had, how time and labor costs had been allocated.

Table LVIII--Metal Cost of Castings Produced from a 100 Pound Melt

Cost of Charge:

Malleable pig iron	(85.00 lbs at $64.25/gross ton)	$24.38
No. 1 heavy steel scrap	(5.5 lbs at $27.00/gross ton)	0.066
50% Fe-Si	(5.0 lbs at $0.138/lb)	0.690
Fe-alloys	(3.25 lbs at $1.41/lb)	4.583
Ca-Si-Mn	(1.25 lbs at $0.38/lb)	0.475
Subtotal		$ 8.252
Less scrap credit*	(69.08 lbs at $37.00/gross ton)	$ 1.141
TOTAL**		$ 7.111

*Based on Chicago area prices as reported in Iron Age, March 17, 1969

**$0.208 per good shot (pair of hemispheres)

Table LIX--Cost of Utilities Required to Make 325 Shots

Electric Power (1.25¢/kwh)		$ 9.25
400 ton die casting machine	149.6 kwh	
Induction furnace - melting	369.6 kwh	
Induction furnace - holding	171.6 kwh	
Die heaters	40.4 kwh	
Sleeve	8.8 kwh	
Total	740.0 kwh	
Water (29¢/1,000 gal)		5.69
400 ton die casting machine	5,760 gal	
Induction furnace	13,860 gal	
Total	19,620 gal	
Natural Gas for Ladle Heater (estimated)		0.56
TOTAL*		$15.50

*$0.055 per good shot

Table LX--Cost of Supplies
Consumed While Producing 1938 Good Shots

Description	Quantity Consumed	Cost per Unit (dollars)	Total Cost (dollars)
MgO crucibles	3	35.44	106.32
Refractory grain	3 bags	17.04	51.12
Refractory wash	8 lbs	0.93	7.44
Acetylene	1 tank	14.15	14.15
Plunger lubricant	15 gal	2.85	42.75
Fuel for lift truck	2 gal	0.15	0.30
Malleable iron ladles	32	1.85	59.20
Skimmers for slag	40	1.12	44.80
Asbestos gloves	16 pairs	2.95	47.20
Shot sleeve heaters	8	10.46	83.68
Die heaters	2	20.10	40.20
Cups for CE tester	148	0.32	47.36
Recorder charts	6	2.00	12.00
Disposable thermocouples	436	0.75	327.00
TOTAL*			883.52

*$0.456 per good shot

Table LXI--Summary of the Direct Material Cost for the Pilot Casting Operation

Cost Element	Cost/Shot (dollars)	Cost/Good Shot (dollars)
I. Injection System		
A. Original sleeve w/o liner (Ultimate life: 100,000 shots)	0.01202	
B. Regrinding (three times per 10,000 shots)	0.02850	
C. Liners (one every 10,000 shots)	0.0998	
D. Plungers (one every 500 shots)	0.09600	
Total ÷ (yield adjustment)	0.23632 ÷ .8626 =	0.274
II. Die		
A. First cost (per 2×10^6 shots)	0.005156	
B. Repair cost (30% of original cost per 40,000 shots)	0.07734	
C. Replacement cost (80% of original cost 49 times per 2×10^6 shots)	0.202115	
Total ÷ (yield adjustment)	0.284611 ÷ .8626 =	0.330
III. Cost of the Metal Cast		0.208
IV. Cost of Utilities	0.04769 ÷ .8626 =	0.055
V. Cost of Supplies Consumed		0.456
TOTAL		1.323

Table LXII--Activity Chart for the Pilot Production of Ferrous Die Castings

Time of Day	Operator 1	Time (min)	Operator 2	Time (min)	Melter	Time (min)	Machine	Time (min)
7 am	Clean, patch, & wash crucible Preventive maint. Change plunger	60					Idle	140
8 am	Lubricate die Preheat die & sleeve Material hndlg.	60	Help Operator 1	60	Charge 200 lbs Melt to 2400°F	60		
9 am	Check CE & alloying	20	Inspect castings	20	Super heat, deoxidize	20		
	Cast	15	Control melt temp	15	Supply ladles	30	Run 50 shots	30
	Control melt temp	15	Cast	15				
10 am	Break	15	Break	15	Charge & melt	15	Idle	35
	Super heat melt	20	Deoxidize, skim, cool	20	Break	20		
	Cast	15	Control melt temp	15	Supply Ladles	30	Run 50 shots	30
	Control melt temp	15	Cast	15				
11 am	Weigh alloys	15	Trim castings	15	Charge & melt	15	Idle	35
	Check CE & alloying	20	Inspect casting	20	Super heat, deoxidize	20		
	Cast	15	Control melt temp	15	Supply ladles	30	Run 50 shots	30
	Control melt temp	15	Cast	15				
12 noon	Lunch	30	Lunch	30	Charge 200 lbs Melt to 2400°F	30	Idle	60
	Super heat melt	30	Deoxidize, skim, cool	30	Lunch	30		
1 pm	Cast	15	Control melt temp	15	Supply ladles	30	Run 50 shots	30
	Control melt temp	15	Cast	15				
	Weigh alloys	15	Trim castings	15	Charge & melt	15	Idle	35
2 pm	Check CE & alloying	20	Inspect castings	20	Super heat, deoxidize	20		
	Cast	15	Control melt temp	15	Supply ladles	30	Run 50 shots	30
	Control melt temp	15	Cast	15				
	Weigh alloys	15	Trim castings	15	Charge & melt	15	Idle	35
3 pm	Check CE & alloying	20	Inspect castings	20	Super heat, deoxidize	20		
	Cast	15	Control melt temp	15	Supply ladles	45	Run 75 shots	45
	Control melt temp	15	Cast	15				
	Cast	15	Control melt temp	15				
4 pm	Clean area	35	Material hndlg.	35	Pig out surplus metal	35	Idle	35

Table LXII--Activity Chart for the Pilot Production of Ferrous Die Castings
(Continued)

Summary				
	Operator 1	Operator 2	Melter	Machine
Support operations	7 hrs 15 mins	6 hrs 30 mins	8 hrs	--
Operation of machine	1 hr 45 mins	1 hr 30 mins	--	3 hrs 15 mins
Total time	9 hrs	8 hrs	8 hrs	8 hrs
Utilization of machine	19.5%	18.8%	--	40.7%

Table LXIII--Allocation of Time and Labor Cost for One Day's Pilot Production Run (Based on Activity Chart)

Operation	Time (min)	Total Cost* (dollars)	Cost per Good Shot** (dollars)
Die casting	195	10.40	0.037
Melting	150	8.00	0.029
Melt maintenance	455	24.27	0.088
Ladle preparation	195	10.40	0.037
Material handling	115	6.13	0.022
Trim and inspect (visually)	125	6.67	0.023
Housekeeping	35	1.86	0.007
Preventative maintenance	180	9.60	0.034
Paid breaks	50	2.67	0.009
TOTAL	1500	80.00	0.286

*Based on an hourly wage rate of $3.20

**Based on the day's output of 560 good hemispheres (280 good shots)

The functional makeup of the die casting operation itself was also analyzed in much greater detail than Table LXII would seem to indicate. Dort had a time and motion study made of an entire day's operation during the early part of the pilot production. The findings of that run were then rechecked near the end of the pilot production indicating, as may be seen in Table LXIV, that the operators' proficiency had increased with experience. The sustainable rates were calculated by the specialist who made the time and motion analysis, assuming that the two operators would alternate at the task of casting every 20 minutes; standard formulas for that discipline were applied to make the calculation. The insight which Table LXIV provides concerning the pilot production operation is an invaluable aid in the identification of opportunities for cost reduction, as will be seen later in this report.

The remaining direct labor costs which Dort was able to assemble were peripheral maintenance costs and the cost of service activities. Those costs are compiled in Table LXV.

The maintenance operations for which costs were obtained included crucible maintenance. The melting crucibles employed by Dort were found to be capable of producing only 560 good shots. Three hours of labor at $3.20 per hour was required to replace a crucible.

During the pilot production run, which produced 1938 good shots (pairs of hemispheres), Dort found it necessary to spend 3.5 hours at $3.20 per hour replacing burned out heating elements.

Dort also determined that 31 hours were required to set the die in the machine; that task, too, was performed by the operators paid at a rate of $3.20 per hour. (The time required to remove the die from the machine was not determined.) Returning to the assumption made earlier concerning the period of useful service of the die, it must be assumed that the setup cost, too, would be incurred every 10,000 shots. (To calculate the cost per good shot, of course, the yield adjustment had to be applied.)

Dort determined that the setup of the electrical and control equipment took 17 hours. It was performed by operators paid $3.20 per hour. It has been assumed, however, that the electrical setup cost would be incurred only at the beginning of a production run, i.e., it would be nonrecurring over the course of a production run. When spread over 2,000,000 shots, for example, it disappears. Inasmuch as it is treated in the same way as the first die cost, it might appropriately be added to that cost.

Table LXIV--Results of a Time and Motion Study of the Pilot, Ferrous Die Casting Operation

Average Time (Minutes)

Function	14 March 1969 Operator 1	14 March 1969 Operator 2	20 March 1969 Operator 1	20 March 1969 Operator 2
Lubricate plunger	0.06	0.09	0.06	0.07
Pour	0.10	0.12	0.08	0.10
Inject metal	0.08	0.08	0.08	0.08
Remove casting	0.14	0.15	0.12	0.11
Clean, soot, and close die	0.17	0.15	0.17	0.16
TOTAL	0.55	0.59	0.51	0.52

Summary

	14 March 1969 Cycle Time (min)	14 March 1969 Cycle Rate (shots/hr)	20 March 1969 Cycle Time (min)	20 March 1969 Cycle Rate (shots/hr)
Operator 1	0.55	109	0.51	118
Operator 2	0.59	102	0.52	115
Combined	0.57	106	0.515	116.5
Standard	0.55	109	0.51	118
Sustainable	0.66	91	0.61	98.5

Table LXV--Cost of Peripheral Maintenance and Service Activities

		Cost/Good Shot (dollars)
Maintenance Activities		0.035
Crucible Repair (three hours at $3.20/hour for each 560 good shots)	$0.017	
Replace Cartridge Heaters (3.5 hours at $3.20/hour for 1938 good shots)	0.006	
Reset Die (31 hours at $3.20/hour every 10,000 shots--8626 good shots)	0.012	
Total	$0.035	
Service Activities		2.577
Chemical Analysis (three analyses for nine elements at 65¢/element for 280 good shots)	0.063	
Annealing and Cleaning ($40.33/ton--2941 good shots)	0.014	
Machining (estimated $1.25/hemisphere)	2.500	
Total	2.577	
TOTAL		2.612

The services which Dort employed during the pilot die casting of ferrous hemispheres were: chemical analysis, annealing and finishing, and machining. Dort's contract with a Chicago-area analytical laboratory stipulated a charge of $0.65 per determination (one element in one sample). Three complete analyses (nine elements) were performed during the course of a single day. The cost incurred for analytical work was $17.55; 280 good shots (pairs of hemispheres) were produced.

The annealing and finishing operations consisted of a batch ferritizing anneal and a sand blasting operation, both of which were performed on the production facilities of the Moline Malleable Iron Company. The cost of that operation was determined to be $40.33 per ton of castings. The average weight of a pair of good hemispheres was 0.68 pounds or 2941 per ton. The machining operation is incomplete as of this writing. Reliable costs are, therefore, not available; but the difficulties encountered by the organization performing that service were noted in Section VIII. In view of those difficulties, a rather arbitrary value of $1.25 per hemisphere ($2.50 per good shot) has been assigned to the machining operation.

Table LXVI summarizes all of the direct labor costs assigned to the pilot production of die cast malleable iron hemispheres.

3. MANUFACTURING OVERHEAD AND SHOP COST

Manufacturing overhead normally includes the following elements: supervision and the services of specialists, premiums and employee benefits paid to manufacturing personnel, services, supplies, tools, utilities maintenance depreciation, taxes, and insurance. It is obvious from the list that some of those elements normally assigned to manufacturing overhead have been considered elements of direct material or direct labor, for the purpose of this analysis. Supplies, tools, and utilities, for instance, were considered to be elements of direct material cost, whereas services and maintenance were treated as elements of direct labor cost. However, because most of those elements of cost were related directly to production and were unique to the combination of material and configuration being produced, the rather arbitrary shuffling of cost elements can be defended as basically sound.

All but one of the remaining elements of manufacturing overhead are environment-related rather than process-related, i.e., they will differ from city to city and from company to company and will depend

Table LXVI--Summary of the Direct Labor Cost for the Pilot Casting Operation

Cost Element	Cost/Good Shot (dollars)
Unit Operations: (Die casting, melting, melt maintenance, ladle preparation, material handling, trimming and inspection, and paid breaks)	0.245
Peripheral Activities: (Crucible repair, cartridge heater replacement, resetting die, preventative maintenance, housekeeping)	0.076
Service Activities: (Chemical analysis, annealing and cleaning, and machining)	2.577
TOTAL	2.898

on such things as management philosophy, union representation, employee benefit programs, etc. Calculating a manufacturing overhead for Dort, therefore, would not only have been pointless, but it could conceivably have adversely affected the competitive position of the Moline Malleable Iron Company; it was not calculated.

Once total manufacturing overhead costs have been determined, they are normally related to either direct materials costs or direct labor costs for proration. Considering the makeup of the direct materials and labor costs as they have been defined here, it appears that the most equitable proration of manufacturing overhead would be achieved by relating it to direct labor costs. (The direct materials costs included die and melt stock costs which would be inconsistent from part to part.) For this analysis, manufacturing overhead will be related to direct labor costs.

First, however, the one process-related element of manufacturing overhead will be analyzed to determine its impact on shop costs. That element is depreciation, the impact of which is related to the scale of the operation, the degree of automation, and the level of utilization of the capital equipment. (Dort's pilot production effort, for instance, was constrained by inadequate melting capacity. With a greater capital investment, continuous operation could have been achieved; and, rather than amortizing that investment over the output of 3-1/4 hours per shift, it might have been amortized over the output of seven or eight hours per shift.)

The essential elements of a melt-constrained facility, similar to that used for the pilot production phase of the contract, are listed below with an estimate of the initial cost of new equipment and installation. (This is not to be confused with a complete, production, ferrous die casting facility!)

Essential Elements of a Melt-Constrained Die Casting Facility Similar to That Used for the Pilot Casting Operation

Description	First Cost (dollars)	Installation 10% of First Cost (dollars)	Total Cost (dollars)
400 ton die casting machine	50,000	5,000	55,000
100 kw induction power supply and furnace	25,000	2,500	27,500
Ladle heater	1,000	100	1,100
Instrumentation	10,000	1,000	11,000
TOTAL	86,000	8,600	94,600

Normally, equipment of the preceding description would be classified into several different categories, each of which has its own depreciation schedule. Typical schedules are indicated below for each of the pieces of equipment essential to a die casting operation on the scale of the pilot casting operation. The annual depreciation for each piece of equipment has also been calculated on a straight line basis.

Annual Depreciation on the Essential Elements of the Melt Constrained Die Casting Facility

Description	Write-Off Period (yrs)	Annual Depreciation (dollars/yr)
400 ton die casting machine	12	4,583
100 kw power supply and furnace	16	1,719
Ladle heater	8	138
Instrumentation	8	1,375
TOTAL		7,815

Based on a five-day week, that annual depreciation can be translated into a per diem depreciation of \$29.95. Dort's average daily production of good shots for Lots 3 through 8 of the pilot casting operation was 280. Using that per diem production rate, the hypothetical depreciation charge per pair of good hemispheres produced in a two-cavity die on a 400 ton die casting machine in a melt-constrained environment was calculated to be \$0.107. The incentive to increase equipment utilization, to employ multiple cavity dies, and to schedule multiple shift operations is obvious, although the reduction in depreciation costs per piece that might be affected by multiple shift operation would certainly have to be weighed against the possibility of costs being increased by differential wage rates or shift premiums.

The cost of machining the hemisphere, which was included in the direct labor costs, was considered an extraordinary expense. If that cost were deleted, the total direct labor cost for the pilot production of malleable iron hemispheres would have been \$0.398 per good pair of hemispheres. (See Tables LXV and LXVI.) The manufacturing overhead costs were assumed to be 150% of that reduced direct labor cost, or \$0.597 per good shot. (Although the 150% rate was assumed quite arbitrarily, it is representative of the real rate in many industries.) Using that assumed overhead rate, the total shop costs for the pilot casting of malleable iron hemispheres in a two-cavity die were calculated. The results are summarized in Table LXVII.

Table LXVII--Summary of the Estimated Shop Cost for the Pilot Casting Operation

Cost Element	Cost/Good Shot* (dollars)
Total direct material cost	1.323
Total direct labor cost	2.898
Manufacturing overhead**	0.597
TOTAL	4.818

*Two good hemispheres

**150% of the reduced direct labor cost (total direct labor cost less the cost of machining)

4. COST REDUCTION OPPORTUNITIES

Opportunities for cost reduction abounded in the pilot casting operation performed by Dort. The first reaction to this bewildering array of opportunities is to ask, "Where do we start?" One answer is to methodically work through the entire list of cost elements, calculating the cost savings that might be affected by inaugurating each improvement independently. That approach, however, soon leads to a quagmire of confusion. The effects of the individual improvements are not independent; in fact, the individual improvements themselves can often not be instituted independently. Seemingly spectacular savings, that might be affected by making a single improvement, may fade into insignificance when other improvements are considered, and "obviously beneficial" process modifications can often be made only if concomitant changes are made in other practices. The approach here, therefore, will be to describe a facility and mode of operation that might be employed to produce die cast malleable iron hemispheres at a cost lower than that achieved in the pilot casting operation.

First, that die casting facility must have adequate melting capacity to meet the requirements of the process and enough excess capacity to achieve continuous or semi-continuous operation. Temperature control should be automatic.

The incentive to employ automatic, liquid metal transfer is very great, as indicated in Section II of this report. In the short term, however, the probability of successfully achieving reliable, automatic, liquid metal transfer is probably too low to base estimates of process costs on the use of such equipment.

Manual transfer imposes an effective limit on the shot weight, and the maximum permissable shot weight establishes one boundary condition for the design of the die.

The design of the die determines the projected area of the cavity, and the product of that projected area multiplied by the injection pressure determines the minimum lockup force required by the die casting machine, on which that die is to be operated.

The die should be designed to eliminate the hot tearing associated with the solidification shrinkage isolated in the flutes of the hemispheres, so that alloying to alleviate the problem would be unnecessary. The design of the die should also be such that the die cast hemispheres would require no subsequent machining operation, i.e., the mating shoulders and the slot for the locking band would be formed in the casting operation.

To translate these generalities into a model for which costs can be generated, consider first manual transfer. The maximum weight which can practicably be repetitively transferred manually is ten pounds. The average shot weight required to fill the two-cavity pilot production die was 2.15 pounds. Assuming the shot weight to be proportional to the number of cavities in the die, no more than nine cavities could be incorporated in a die. As pointed out in Section VIII, the top and bottom hemispheres are distinctly different; therefore, a ninth hemisphere could only be paired by operating a second machine with a die having an odd number of cavities. The most general solution, therefore, although it is perhaps slightly conservative, would be to design an eight-cavity die.

The next step is to be sure that an eight-cavity die is compatible with the die casting machine available or to ascertain what size machine might be required. There are two criteria, lockup rating and platen area.

A rough calculation of the type indicated below will provide preliminary information concerning the required lockup rating.

Estimate of the Projected Area
of an Eight-Cavity Hemisphere Die

Description	Area (in^2)
Area of cavities* (eight, 2-3/4" dia cavities)	47.6
Area of biscuit (4" dia)	12.6
Area of runners (eight, 1-1/2" x 5")	60.0
Area of overflows (three/cavity x eight cavities, 1/2" x 1-1/2")	18.0
	138.2

*The specified maximum flange diameter of the hemispheres was 2.483".

Dort found 2850 pounds per square inch to be an adequate injection pressure for the two-cavity, pilot production die. Assuming that the same pressure would be satisfactory for an eight-cavity die, the minimum required lockup can be calculated.

Minimum required lockup (tons) =

$$\frac{\text{Projected area (in}^2\text{) x Injection pressure (psi)}}{2{,}000} = 197 \text{ tons}$$

From the standpoint of lockup rating, therefore, it appears that it would be conceptually possible to operate an eight-cavity hemisphere die on a 200 ton machine. Operating an eight-cavity die on a 400 ton machine would represent more conservative practice, however.

Several alternative spatial arrangements for an eight-cavity die were layed out, to determine if they were compatible with the platen area and shot sleeve location of a generalized, 400 ton, horizontal die casting machine--they were. Figure 172 is an example of one such layout.

The die casting machine should also incorporate automatic plunger lubrication.

Assuming again that the shot weight would be proportional to the number of cavities in the die, the shot weight for an eight-cavity die would be 8.6 pounds. The highest cycle rate attained in the pilot casting operation was 117 shots per hour, or 1006.2 pounds per hour in an eight-cavity die. This figure might be used as a basis for sizing a melting facility, but a more conservative approach would be to make allowance for reductions that might be affected in the cycle rate in the future. At a casting rate of 180 cycles per hour, for instance, the melting facility would require a minimum melting capacity of 1548 pounds per hour. The rule of thumb is that each kilowatt of induction melting capacity will melt two pounds of steel per hour. Using this figure for cast iron, it is estimated that 774 kw of melting capacity should be provided. (Inasmuch as cast iron has a significantly lower liquidus than steel, the above estimate is undoubtedly high, introducing another note of conservatism.)

Single furnace practice, like that used for the pilot production of die cast malleable iron hemispheres, would be untenable in a true production situation. Check analyses and adjustments to chemistry

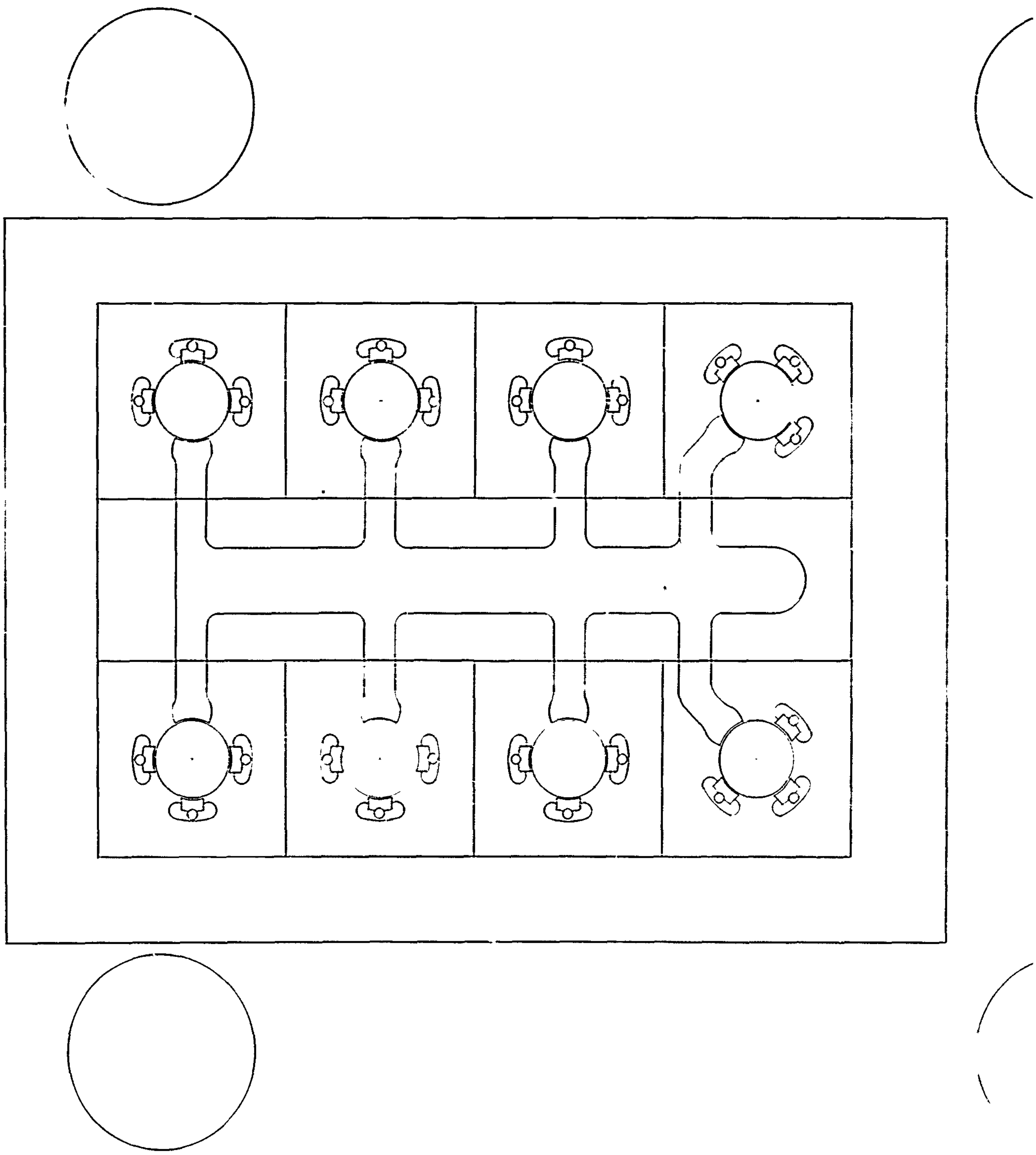


Figure 172: Hypothetical layout for an eight-cavity hemisphere die on a generalized, 400 ton, horizontal die casting machine.

must be made without interrupting or delaying the operation, and the period of time that the metal is held in the molten state must be minimized. To satisfy those objectives, and to attain semi-continuous operation, the facility should have either separate melting and holding furnaces or two or more combination melting-holding furnaces. The former solution is probably preferable for multiple-machine operations, the latter for single-machine operations.

Precise control of the melt temperature is essential to the production of dimensionally accurate parts, but the process cannot afford the full-time services of a man to perform that function. Continuous, automatic, feedback control requires a system for continuously monitoring the melt temperature. Only the discovery of a satisfactory thermocouple protection tube or a satisfactory closed-end sight tube for a radiation pyrometer is required to put such a system into effect, and the work of the Research and Development Center in that area (reported earlier in this report) is very promising.

The cost reductions affected by the equipment and mode of operation just described can now be examined.

With reference to Table LXVIII, consider first the direct material costs. The fourfold increase in the weight of the metal cast was assumed to require an injection syst[illegible] twice as large in diameter and only twice as costly as the one used in [illegible] pilot production. The life of the system was assumed to be related to a n[illegible]r of shots, rather than weight of metal cast. The cost of regrinding sleeves was assumed to be unaffected by the increased inner diameter. It was further assumed that replacing the beryllium-copper plungers with H-13 plungers, at no increase in price, would treble plunger life.

It was assumed that the first cost of the die, the cost of repairing the die, and the cost of replacing the die would be proportional to the number of cavities. The cost per good hemisphere, therefore, would remain unchanged.

It was assumed that an improved die design would negate the need for alloying additions in the cast iron. The ferro silicon and ferro alloy additions could therefore be deleted, reducing the cost per pair of good hemispheres from \$0.208 to \$0.060. That cost could be reduced to \$0.023 per pair of good hemispheres by employing scrap only. As pointed out earlier in this report, however, the probability of producing off-chemistry heats when using scrap practice in a small melting facility is very high. What is required for the die casting process is a melting practice that permits the die caster to melt and pour, without the necessity of making adjustments in chemistry and with the absolute assurance that the analysis will be correct. A malleable iron practice, based on the use of malleable pig iron and steel scrap, comes much closer to that standard than does a scrap iron practice. For this analysis, therefore, the possibility of using iron scrap was disregarded.

Table LXVIII--Potential Cost Savings to be Realized in the Die Casting of Malleable Iron Hemispheres

Cost Element	Cost/Pair of Good Hemispheres (dollars) Pilot Production	Model	Savings/Pair of Good Hemispheres (dollars)
I. DIRECT MATERIAL COSTS			
A. Injection System			
1. Sleeve w/o liner	0.01393	0.00697	0.00696
2. Regrinding	0.03303	0.00826	0.02477
3. Liners	0.11570	0.05785	0.05785
4. Plungers	0.11129	0.01855	0.09274
Total	0.27395	0.09163	0.18232
B. Die	0.32994	0.32994	--
C. Cost of Metal	0.208	0.060	0.148
D. Utilities			
1. Electricity for die casting machine	0.00668	0.00167	0.00501
2. Gas for ladle heaters[a]	0.00200	0.00020	0.00180
3. All others	0.04632	0.04632	--
Total	0.05500	0.04819	0.00681
E. Supplies			
1. Crucibles & refractories	0.08124	0.00976	0.07148
2. Plunger lubricant	0.02206	0.01103	0.01103
3. Malleable iron ladles	0.03055	0.01146	0.01909
4. Asbestos gloves	0.02436	0.00609	0.01827
5. Cups for CE tester	0.02444	0.00306	0.02138
6. Recorder charts	0.00619	0.00155	0.00464
7. Disposable thermocouples	0.16873	0.02435	0.14438
8. All others	0.09832	0.09832	--
Total	0.45589	0.16562	0.29027
TOTAL DIRECT MATERIAL COSTS	1.32278	0.69538	0.62740

[a] Based on an assumed casting rate of 100 cycles per hour, 2760 pairs of good hemispheres per eight-hour shift

Table LXVIII--Potential Cost Savings to be Realized in the Die Casting of Malleable Iron Hemispheres (Continued)

Cost Element	Cost/Pair of Good Hemispheres (dollars) Pilot Production	Model	Savings/Pair of Good Hemispheres (dollars)
II. DIRECT LABOR COSTS[b]			
A. Unit Operations			
1. Die casting	0.03714	0.00928	0.02786
2. Melting & melt maint	0.11524	0.01043	0.10481
3. Ladle preparation	0.03714	0.00928	0.02786
4. Material handling	0.02190	0.00309	0.01881
5. Trimming	0.00857	0.00214	0.00643
6. Inspection	0.01524	0.01524	--
7. Retrim[c]	2.50000	0.00857	2.49143
8. Preventative maint	0.03429	0.00464	0.02965
9. Paid breaks	0.00952	0.00290	0.00662
Total	2.77904	0.06557	2.71347
B. Maintenance Activities			
1. Crucible repair	0.01714	0.00348	0.01366
2. Replace cartridge heaters	0.00578	0.00101	0.00477
3. Reset die	0.01150	0.00288	0.00862
Total	0.03442	0.00737	0.02705
C. Service Activities			
1. Chemical analysis	0.06268	0.00424	0.05844
2. Annealing & cleaning	0.01371	0.01371	--
3. Housekeeping	0.00667	0.00271	0.00396
Total	0.08306	0.02066	0.06240
TOTAL DIRECT LABOR COST	2.89652	0.09360	2.80292
Manufacturing Overhead	0.597	0.14040[d]	0.45660
TOTAL SHOP COSTS	4.81630	0.92938	3.88692

[b] Based on an eight-hour shift, a cycle rate of 100 shots per hour, and a wage rate of $3.20 per hour

[c] Replacing the machining operation in the pilot casting

[d] 150% of total direct labor cost, including the retrimming operation

Reference to Table LVII indicates that Dort experienced unrecoverable losses of 2.20% due to oxidation and 5.46% due to handling. Automatic liquid metal transfer should virtually eliminate such handling losses and reduce oxidation losses appreciably. Clay graphite ladles, too, should reduce the handling losses. The gross savings calculated for an automatic transfer device in Section II included a reduction of $0.0148 per pair of good hemispheres, based on eliminating the handling loss but continuing to produce castings from the high-cost, alloyed malleable iron. If the castings were made from unalloyed malleable iron, a saving of only $0.005 per pair of good hemispheres could be realized by eliminating the handling loss, whereas if the castings were made from scrap iron, the potential saving would dwindle to $0.003. This example reinforces the warning that seemingly spectacular savings predicted for individual cost reduction efforts may often become insignificant when other cost reduction measures are put into effect. Of more importance, however, is the demonstration that whereas a large capital investment might be justified to increase the material yield of a high-priced alloy, very little investment could be justified to increase the yield of a low-cost alloy.

The cost of utilities will, in general, be approximately proportional to the weight of metal cast. Two exceptions to that rule are the electricity required to operate the die casting machine (which is proportional to the number of cycles) and the natural gas required to operate the ladle heater (which is time dependent only).

Several items falling within the category of supplies provided opportunities for cost reduction. Although to a first approximation, the crucible and refractories costs should be proportional to the weight of metal cast, i.e., the number of cavities in the die, there was reason to believe that the costs incurred for crucibles and refractories during the pilot casting operation were unnecessarily high.

MgO is a good, general-purpose, high-temperature refractory. Because it is a basic refractory, however, MgO is not particularly durable in contact with the siliceous slags characteristic of cast irons, and its high temperature capabilities are of no particular benefit when it is used to melt cast irons which have relatively low liquidus temperatures. Two less expensive refractories which are commonly used to melt cast iron are clay graphite and silica. Not only is silica much less expensive than MgO, but, because of its chemical compatibility with siliceous slags, it can be expected to provide longer service than MgO. Therefore, the crucible and refractory costs for the model were based on the use of cast SiO_2.

The approach taken was to calculate the cost of installing SiO_2 refractories on the pilot production scale, assuming that, instead of the useful life of 16 hours, i.e., 650 shots, realized with the MgO crucibles, the SiO_2 crucibles would have a useful life of 32 hours (1300 shots or 1121 pairs of good hemispheres in the pilot operation). The cost per good hemisphere was then assumed to be equally valid for a larger facility. At $.08 per pound, only $10.94 worth of SiO_2 would be required to cast a lining for a 200 pound capacity furnace that would replace both the MgO crucible and the refractory grain used in the pilot casting operation.

It was assumed that twice as much plunger lubricant would be required per shot to make four times as many pairs of good hemispheres in the model operation.

It was also assumed that the larger malleable iron ladles would cost 50% more but would produce four times as many pairs of good hemispheres in the model operation than in the pilot production.

The attrition experienced to asbestos gloves was assumed to be a function of the number of shots only. Therefore, the same number of gloves that was required in the pilot operation would produce four times as many good hemispheres in the model operation.

The number of carbon equivalent determinations required was assumed to be dependent only on the number of shots made (within a relatively narrow range of production rates). It was further assumed that, with increasing proficiency, only half as many determinations per shot would be required in the model operation as were required in the pilot operation. To produce the same number of good parts, therefore, it was assumed that the number of cups required for carbon equivalent determinations in the model operation would be a factor of eight less than the number required in the pilot operation.

The consumption of chart paper was assumed to be proportional only to the number of shots made. The consumption per pair of good hemispheres experienced in the pilot operation would, therefore, be reduced by a factor of four in the model operation.

To achieve automatic control of the melt temperature, the model would dispose of disposable thermocouples. Two possibilities were explored: a noble metal thermocouple, insulated with Al_2O_3 and protected by concentric tubes of Al_2O_3 and mullite, and a closed-end, mullite sight tube for an automatic radiation or two-color pyrometer.

It was assumed that the noble metal thermocouples would be 3' long, made from 0.020" diameter wire, insulated with 1/8" diameter alumina tubing, with a 0.040" diameter double bore. The insulated thermocouples would then be inserted in concentric, closed-end protection tubes of Al_2O_3 (5/16" OD x 1/16" wall) and mullite (1/2" OD x 1/16" wall). When failure occurred, it was assumed that a 1' length of each of the thermocouple legs could be salvaged for scrap credit. Two different thermocouple combinations were assessed, platinum/platinum-10% rhodium and platinum-6% rhodium/platinum-30% rhodium.

It was also assumed that a 2" OD x 1/8" wall, closed-end, mullite tube would make a satisfactory sight tube for an automatic pyrometer.

The cost per pair of good hemispheres was calculated for the two alternative approaches, based on a production rate of 100 shots per hour in an eight-cavity die. Price data for the ceramic components required by the two alternatives were taken from a Coors Porcelain Company price list, dated 15 February 1969. Prices for the noble metals were supplied by Engelhard Industries. The results of the calculation are presented below.

Cost of Automatic Temperature Sensing

	Cost (Dollars/Pair of Good Hemispheres)*		
Approach	4-hr Life	8-hr Life	16-hr Life
Pt/Pt-10% Rh thermocouple	0.053	0.027	0.013
Pt-6% Rh/Pt-30% Rh thermocouple	0.058	0.029	0.014
Mullite sight tube	0.024	0.012	0.006

*Based on 100 shots per hour and a yield of 86.26%

A sight tube used in conjunction with an automatic pyrometer seems to be the best choice for the model. A life of only four hours was assumed.

The cost per pair of good hemispheres for all of the remaining items of supply was assumed to be the same for the model as it had been for the pilot casting operation.

To complete the analysis, it is necessary to assign direct labor costs to the model that has been hypothesized. This is a very difficult task which, in the end, must be done somewhat arbitrarily.

Although Dort reported the division of time among the individual elements of the pilot casting operation, attempts to make projections based on those data reveal marked incongruities. For example, Dort reported allocating 115 minutes to the handling of material for the pilot production of 650 hemispheres. The model operation would produce 9.85 times as many hemispheres in one shift (i.e., 6400 hemispheres). If the time required for material handling was assumed to be proportional to the number of cavities in the die, the material handling estimate for the model would be 1132.3 minutes, or 18.9 man-hours per eight-hour shift. The difficulty, as Parkinson noted, is that work expands to fill the time available. The time a task takes depends on the effort expended, the proficiency of the operator, the degree of automation, and many less tangible factors.

One of the easier calculations is to determine the effect of installing automatic plunger lubrication on the die casting machine. Referring to Table LXIV, it may be seen that such a modification would reduce the standard cycle length by a minimum of 0.06 minutes, from 0.51 to 0.45 minutes; or, restating the results, it would increase the standard production rate from 117.6 to 133.3 shots per hour. This indicates that the model, which is based on a cycle rate of 100 shots per hour, is conservative, indeed.

The model envisions die casting proceeding uninterrupted for eight hours a day.

By introducing automatic temperature control and reducing the number of carbon equivalent determinations required, it should be possible to combine the melting and melt maintenance functions. In a one- or two-shift operation, melting should begin 30 minutes before casting begins. The model also assumes that the melt requires maintenance 30 minutes after casting has been discontinued.

For the model, ladle preparation was assumed to require the same period of effort as the casting operation. (That was also found to be true in the pilot casting operation.)

The allocation of time for material handling in the model presented a problem, as indicated earlier. Assuming that a day's production would require 16, 30-minute melts, 10 minutes per melt was arbitrarily allotted to material handling.

The trimming operation associated with the pilot production of the hemispheres consisted simply of breaking the overflows from the castings and the castings from the runners. It was assumed that the introduction of a trim press for the model operation that would break, not shear, the in-gates and overflow gates would enable eight castings to be trimmed in the same period required for two in the pilot casting operation.

So that the gates could readily be removed from the pilot production hemispheres in the subsequent machining operation, Doehler's design provided for both the in-gates and the overflow gates to communicate only with the mating surfaces of the hemisphere bases. The model, however, proposes to eliminate the machining operation. To accomplish gate removal, the gates would be designed to communicate only with those surfaces of the locking flange or flutes that are perpendicular to the base of the hemisphere. A second trimming or punching operation, subsequent to the annealing operation, would then shear the gates off. The time per hemisphere for that second operation was assumed to be the same as the time per hemisphere for the single trimming operation associated with the pilot production.

The time requirements per hemisphere for inspection were assumed to be the same for the model as for the pilot operation.

The model consigned housekeeping to the category of a service.

Quite arbitrarily, it was assumed that the model operation would require one more man-hour of preventative maintenance than the pilot casting operation had.

The time spent on the various functions just described in the course of the pilot production are compared on the following page, with the time allocated for the same functions in the model. Quite surprisingly, the model requires a ten-man crew, as opposed to only three for the pilot operation. The time required by the model for paid breaks, therefore, would be 150 minutes, versus only 50 minutes for the pilot casting operation. The cost of the paid breaks, however, would be spread over the production of 2760 pairs of good hemispheres in the model operation, whereas it was spread over the production of only 280 pairs of good castings in the pilot casting operation.

Time Allocated for Unit Operations

Function	(Minutes) Pilot Production	Model
Die casting	195	480
Melting and melt maintenance	605	540
Ladle preparation	195	480
Material handling	115	160
Trimming	45	443.5
Inspection	80	788.5
Retrim	*	1,774
Preventative maintenance	180	240
Paid breaks	50	150
TOTAL	1,465 (3.05 laborers)	5,106 (10.64 laborers)

*A service function in the pilot casting operation

Turning to the maintenance activities associated with the die casting of malleable iron hemispheres, it was estimated for the model that six man-hours would be required to reline the larger furnaces with the castable SiO_2. It was further assumed that the model would require the simultaneous operation of two furnaces, each of which would require relining every 32 hours. The combined output of the two furnaces for that period would be 11,041 good pairs of hemispheres. The labor cost of relining the crucibles, therefore, would be $0.00348 per good hemisphere.

The failure of cartridge heaters was assumed to be proportional to the number employed and their length of service, in hours. It was assumed that the eight-cavity model die and the associated shot sleeve would require twice as many heaters as the pilot production die and sleeve. Therefore, in a 64-hour period comparable to the period during which Dort performed the pilot casting operation, seven hours would be required to replace cartridge heaters, but 22,082 pairs of good hemispheres would be produced.

The die would require resetting in the model operation after the same number of cycles as in the pilot operation, but the eight-cavity die would have produced four times as many hemispheres during that period.

Proceeding to the service activities, the model operation would require a check analysis of the chemistry only twice daily, while producing 2760 good hemispheres. Because the model operation would employ unalloyed malleable iron, the analyses would not necessarily have to be so complete as those performed during the pilot operation. No adjustment has been made in Table LXVIII on that account, however.

The annealing and cleaning costs per hemisphere would be identical for the model operation and the casting operation.

It was assumed that performance of the housekeeping necessary for the model operation would require four times the effort per eight-hour shift required by the pilot operation. For the model operation, however, the production of good pairs of hemispheres during that period would be 2760, versus 280 for the pilot operation.

After the adjustment for a manufacturing overhead rate was made, the total shop cost was found to be $0.929 per pair of good hemispheres, as reported in Table LXVIII.

In the following paragraphs, we will look at some cost projections based on other models.

5. COST PROJECTIONS

The opportunities for cost reduction revealed in the preceding pages are impressive, but perhaps not sufficient to make die cast malleable iron hemispheres competitive with their aluminum counterparts on the basis of cost alone. A man of persistence and perception, however, will not accept the cost data generated for the preceding model as conclusive and dismiss the proposition but will insist that the effect of other process modifications be explored. No one who has laboriously toiled through such an analysis could greet that prospect with enthusiasm, but it is, unquestionably, an important aspect of the introduction of every new process.

To eliminate the drudgery of making such repetitive cost analyses, the Lamp Metals and Components Department has begun to make use of the computational power of an electronic computer.

Dr. D. T. Hurd, who has lead the way in the use of the computer for projecting the costs of the ferrous die casting process, prepared a program specifically to calculate the shop cost of die cast hemispheres.

That program, which is exhibited in Table LXIX, was written in BASIC language for use on the General Electric Company Time Sharing Service.* Table LXX is a key to the variables incorporated in the cost computing program. Table LXX also assigns three sets of values to the independent program variables which, in effect, represent three different mathematical models of the production process.

The set of values selected for Run 1 were inspired by the previously postulated model. The program, however, requires more detail concerning the elements of manufacturing overhead than that model employed. For example, the program assigns to Supervision the wage rate of a working foreman (A) and requires an estimate of the daily depreciation charges for the facility (E). Several process parameter changes were also envisioned in the set of values assigned to the variables in Run 1: a two-shift, rather than a one-shift, operation was postulated (H); an improvement in metal efficiency, from 27% to 30% (Q); and an improvement in yield, from 86.26% to 90% (Y) were foreseen, and it was assumed that the casting and melting operation could be performed by three men plus the foreman.

For any given run, all values were fixed except the first die cost, the period of repair-free service of the die, and the ultimate output of the die. Combinations of two levels of first die cost ($20,000 and $40,000), four levels of repair-free service (5,000, 10,000, 15,000, and 20,000 shots), and four levels of ultimate output (10,000, 100,000, 1,000,000 and 10,000,000 hemispheres) were investigated. The results for Run 1 have been transcribed in Table LXXI. Trivial solutions, which the computer prints out faithfully, have been deleted, e.g., the shop cost for a 20,000 shot period of repair-free die life (160,000 hemispheres) but an ultimate life of only 10,000 hemispheres.

The data in Table LXXI indicate that, under the least favorable circumstances (i.e., a first die cost of $40,000, a period of only 5,000 shots of repair-free service, and an ultimate production of only 100,000 hemispheres), the shop cost per pair of good hemispheres would be $1.303. Under the most favorable combination of circumstances, the shop cost per pair of good hemispheres might be as low as $.260.

For Run 2, it was assumed that, by increasing the capital investment (E), the production rate (R) could also be increased. The cost of rent, heat, and light was also predicted to increase (U). Larger furnaces which would be more expensive to reline were foreseen (G), but an improved furnace life of 80 hours, versus 32 hours, was predicted (O). Scrap melting practice was to be employed (M).

*Available to the general public through the General Electric Company Information Service Department, Bathesda, Maryland.

Table LXIX--Computer Program Written in BASIC Language to Calculate the Shop Cost of Producing Pairs of Die Cast Hemispheres

```
10 PRINT "D","L","Z","K"
20 READ A,B,E,G,H,I
22 READ J,M,N,Ø,P,Q,R
24 READ S,T,U,W,Y
30 FØR D=2000000TØ4000000STEP2000000
35 FØR L=5000TØ20000STEP5000
40 FØR C=4TØ7STEP1
45 LET Z=10↑C
50 LET F=(T/(L*N))+((E+U+(W*H*P)+(A*H))/(H*R*N))
52 LET Z1=Z/(N*L)
56 LET D1=(D*(1+(INT(Z1))*I))/Z
60 LET V=((M*J)/Q)+D1+B+J+(G/(Ø*R*N))+(S/N)
70 LET X=((F+V)/Y)-((1-Y)*J*M/2)
75 LET K=2*X
80 PRINT D,L,Z,K
90 NEXT C
92 NEXT L
94 NEXT D
120 DATA 500,2,12000,4000,16,.1
122 DATA .34,3.2,8,32,320,.3,100
124 DATA 9,20000,2000,3,.9
130 END
```

Notes:

a. Asterisks are an instruction to perform a multiplication

b. A/N quite conventionally represents A ÷ N

c. A↑N symbolizes A raised to the Nth power

d. INT (Z1) is an instruction to select the greatest integer not greater than (Z1)

Table LXX--Key to the Variables in the Program for Computing Shop Costs for Die Casting Ferrous Hemispheres

Variable	Explanation	Value Assigned		
		Run 1	Run 2	Run 3
A.	Supervision--hourly wage of a working foreman (cents)	$5.00	$5.00	$5.00
B.	Cost to trim, anneal, clean, finish, and inspect (cents per hemisphere)	$ .02	$ .02	$ .02
C.	Mathematical parameter	--	--	--
D.	Initial cost of die set (cents)	$20,000 and $40,000		
E.	Capital charge for facilities (cents per day)	$120.00	$150.00	$150.00
F.	Mathematical parameter	--	--	--
G.	Cost of relining a furnace (cents)	$ 40.00	$ 50.00	$ 50.00
H.	Hours of operation per day	16 hrs	16 hrs	24 hrs
I.	Cost of reworking dies divided by first cost of die (decimal fraction)	10%	10%	10%
J.	Weight of hemisphere (pounds)	0.34 lbs	0.34 lbs	0.34 lbs
K.	Shop cost per pair of good hemispheres (cents)	Dependent variable being computed		
L.	Repair-free service of die (cycles)	5,000, 10,000, 15,000, & 20,000 shots		
M.	Cost of metal cast (cents per pound)	$.032	$.019	$.019
N.	Number of cavities per die	8	8	8
O.	Useful life of furnace lining hours	32 hrs	80 hrs	80 hrs
P.	Wage rate (cents per hour)	$3.20	$3.20	$3.20
Q.	Metal efficiency--weight of hemispheres produced divided by weight of metal cast (decimal fraction)	30%	30%	30%
R.	Cycle rate (shots per hour)	100	180	180
S.	Cost of metal injection (cents per shot)	$.09	$.09	$.09
T.	Cost of removing and replacing die for rework (cents per repair cycle)	$200.00	$200.00	$200.00
U.	Rent, heat, and light (cents per day)	$20.00	$36.00	$45.00
V.	Mathematical parameter	--	--	--
W.	Manning--number of laborers	3 men	3 men	3 men
X.	Shop cost per hemisphere	Dependent variable being computed		
Y.	Process efficiency--good parts divided by total parts (decimal fraction)	90%	90%	90%
Z.	Number of hemispheres produced during the life of the die	10,000, 100,000, 1,000,000, & 10,000,000		

Table LXXI--Computer Projection of the Shop Cost to Die Cast Malleable Iron Hemispheres--Run 1

First Die Cost (dollars)	Period of Repair-Free Die Life (shots)	Ultimate Output of Die (hemispheres)	Shop Cost per Pair of Good Hemispheres (cents)
20,000	5,000	100,000	76.9282
20,000	5,000	1,000,000	39.1505
20,000	5,000	10,000,000	35.1505
20,000	10,000	100,000	71.9282
20,000	10,000	1,000,000	32.8171
20,000	10,000	10,000,000	29.0393
20,000	15,000	1,000,000	30.8542
20,000	15,000	10,000,000	26.9875
20,000	20,000	1,000,000	29.8727
20,000	20,000	10,000,000	25.9616
40,000	5,000	100,000	130.262
40,000	5,000	1,000,000	54.706
40,000	5,000	10,000,000	46.706
40,000	10,000	100,000	120.817
40,000	10,000	1,000,000	42.5949
40,000	10,000	10,000,000	35.0393
40,000	15,000	1,000,000	38.8542
40,000	15,000	10,000,000	31.1208
40,000	20,000	1,000,000	36.9838
40,000	20,000	10,000,000	29.1616

The projections based on the set of values assigned the variables for Run 2 may be found in Table LXXII. The process modifications envisioned have reduced the cost per pair of good hemispheres to $1.243 under the least favorable combination of circumstances. Under the most favorable conditions, however, a pair of good hemispheres might be produced for as little as $.200.

The set of values assigned to the variables for Run 3 differed from those for Run 2 only with respect to the scheduling of the operation (H) and the cost of rent, heat, and light (U). A three-shift operation was forseen. As may be seen from the data compiled in Table LXXIII, those changes affected only a very slight change in the shop cost per pair of good hemispheres.

The minimum shop cost projected by the three computer runs was $.195 per pair of good hemispheres (which was projected by Run 3 for a $20,000 die that would produce five million pairs of hemispheres during its lifetime and require repair only every 20,000 shots). To achieve a shop cost that low would certainly be an ambitious project, considering the nature of the assumptions made; but such an achievement is, unquestionably, too, an extremely exciting prospect.

Table LXXII--Computer Projection of the Shop Cost to Die Cast Malleable Iron Hemispheres--Run 2

First Die Cost (dollars)	Period of Repair-Free Service (shots)	Ultimate Output of Die (hemispheres)	Shop Cost per Pair of Good Hemispheres (cents)
20,000	5,000	100,000	71.0085
20,000	5,000	1,000,000	33.2308
20,000	5,000	10,000,000	29.2308
20,000	10,000	100,000	66.0085
20,000	10,000	1,000,000	26.8974
20,000	10,000	10,000,000	23.1197
20,000	15,000	1,000,000	24.9345
20,000	15,000	10,000,000	21.0678
20,000	20,000	1,000,000	23.953
20,000	20,000	10,000,000	20.0419
40,000	5,000	100,000	124.342
40,000	5,000	1,000,000	48.7863
40,000	5,000	10,000,000	40.7863
40,000	10,000	100,000	114.897
40,000	10,000	1,000,000	36.6752
40,000	10,000	10,000,000	29.1197
40,000	15,000	1,000,000	32.9345
40,000	15,000	10,000,000	25.2011
40,000	20,000	1,000,000	31.0641
40,000	20,000	10,000,000	23.2419

Table LXXIII--Computer Projection of the Shop Cost to Die Cast Malleable Iron Hemispheres--Run 3

First Die Cost (dollars)	Period of Repair-Free Service (shots)	Ultimate Output of Die (hemispheres)	Shop Cost per Pair of Good Hemispheres (cents)
20,000	5,000	100,000	70.4684
20,000	5,000	1,000,000	32.6906
20,000	5,000	10,000,000	28.6906
20,000	10,000	100,000	65.4684
20,000	10,000	1,000,000	26.3573
20,000	10,000	10,000,000	22.5795
20,000	15,000	1,000,000	24.3944
20,000	15,000	10,000,000	20.5277
20,000	20,000	1,000,000	23.4129
20,000	20,000	10,000,000	19.5018
40,000	5,000	100,000	123.802
40,000	5,000	1,000,000	48.2462
40,000	5,000	10,000,000	40.2462
40,000	10,000	100,000	114.357
40,000	10,000	1,000,000	36.1351
40,000	10,000	10,000,000	28.5795
40,000	15,000	1,000,000	32.3944
40,000	15,000	10,000,000	24.661
40,000	20,000	1,000,000	30.524
40,000	20,000	10,000,000	22.7018

SECTION X

CONCLUSIONS AND OBSERVATIONS

This section summarizes those conclusions, convictions, impressions, and observations that resulted from the work reported in the preceding pages. That work was large in magnitude, long in duration, loosely organized, and, by design, redundant and iterative. As noted earlier, each of the participating project teams was given the maximum possible latitude to find independent solutions to the same problem, and they did. The best of those solutions, modified and supplemented by the experience gained in the course of the project, were employed in the pilot casting operation. That operation provided new and expanded perspectives of the problems and potential of the ferrous die casting process. Even the cost analysis provided additional insight into several facets of the process.

What resulted, therefore, was, in effect, a continuous revelation of seemingly random observations. To put this hodgepodge of information into a meaningful perspective, an attempt has been made to assign each bit and piece to one of six different classifications:

a. Melting

b. Liquid metal transfer

c. Pressure injection

d. Die design

e. Casting quality

f. Cost.

Within each classification will be found general observations and information specifically pertinent to the production of die cast hemispheres. Each classification will also embrace ideas concerning equipment, materials, and techniques.

1. MELTING

For small ferrous die casting facilities, high-frequency induction melting is probably the only practical alternative. A good rule of thumb for sizing such furnaces is that each kilowatt of melting capacity is capable of melting two pounds of steel per hour. To provide a continuous or semi-continuous supply of molten metal, a minimum of two

furnaces is required. One furnace may be used exclusively for melting and the other exclusively for holding, or they may both be combination melting and holding furnaces. The furnaces should be scaled to minimize changes in the composition of the melt.

Changes in the composition of ferrous alloy melts, as a function of time, were noted in the course of this project. The elements lost were carbon, chromium, silicon, and manganese (although it is assumed that other elements, the oxides of which have a high negative free energy of formation, would have suffered the same fate). The loss of carbon was more rapid and more troublesome in steels and slower and less troublesome in cast irons, simply because the steels, with their higher liquidus temperatures, require higher melt temperatures than do the cast irons. At any given temperature, a high chromium content was observed to stabilize the carbon content, either by reducing the activity of the carbon or by being preferentially oxidized itself, e.g., the carbon loss from AISI 403 stainless steel was significantly less than the loss from AISI 4340--both having approximately the same melt temperature. It was also demonstrated that AISI 304 stainless steel, which has no intentional carbon addition, could be held in the molten state for up to six hours and still remain within the allowable composition range--although significant losses of chromium and manganese were observed. Covers of flowing argon gas were investigated and found to be of little help in reducing either slag formation or the preferential loss of one or more alloying elements. The introduction of an automatic molten metal transfer device, of either the gravity-feed or pneumatic type, however, would permit a positive pressure of an inert gas, on the order of a few inches of water, to be continuously maintained over the melt and should be most effective in reducing oxidation losses. That option is not available for operations dependent on hand ladling, however. The general conclusion that may be drawn from the work reported here is that the ferrous die casting facility which must depend on hand ladling should be scaled, so that metal will never be held in the molten state for more than 30 minutes.

The cost study performed as part of this project clearly illustrated the economic incentive to adopt scrap melting practices, but the experience with scrap melting was unfavorable. The scrap melts made in the course of the project were invariably off-chemistry. It was concluded that scrap melting should be undertaken by a small die casting operation only if a very reliable source of scrap, such as in-house scrap, were available. For an operation large enough to have a central melting facility and a direct-reading spectrograph, scrap melting is much more attractive. (Such a central melting facility might employ arc furnaces or large induction furnaces; or, in the case of cast iron only, it might employ cupolas.)

MgO is a good general-purpose refractory in which the entire range of ferrous alloys may be melted, and it is the best selection for melting steel. For an operation in which cast iron only is to be melted, however (e.g., the large-scale production of malleable iron hemispheres), MgO is not the best choice. Clay graphite and silica refractories are less expensive than MgO and more resistant than MgO to attack by the siliceous slags formed on cast iron melts.

Variations in the melt temperature may be reflected by variations in the weight and dimensions of the castings. Unnecessarily high melt temperatures may accelerate the deterioration of the injection system and the dies and may introduce soldering problems. High melt temperatures also promote slag formation and compositional changes. Melt temperatures too close to the liquidus may result in castings with poor surfaces, or may even interfere with the filling of the die. For these reasons, close, continuous or semi-continuous control of melt temperature is indispensible to the ferrous die casting process. Optical pyrometer readings must be corrected for low emissivity values, and their accuracy is affected by slag covers and thin oxide films. The use of disposable or semi-disposable platinum/platinum-rhodium immersion thermocouples in fused silica protection tubes imposes an unacceptable cost burden on the process. It was concluded that the best answer to the problem was to use closed-end sight tubes of mullite or zircon, the closed end of which could remain continuously submerged in the melt. These tubes would permit the temperature to be determined accurately with an optical or radiation pyrometer for either manual or automatic temperature control. The requirement that temperature control be continuous or semi-continuous makes automatic control the only economically practical alternative.

2. LIQUID METAL TRANSFER

At this time, the only reliable way of transferring molten ferrous alloys from a holding furnace to the pressure injection system of a die casting machine is by hand ladling. Ladles of clay graphite, malleable iron, and stainless steel were employed in the course of this project. All were preheated to red heat in gas-fired ladle heaters to minimize skull losses.

To prevent molten metal from welding to the metal ladles, it was found necessary to coat them. The coating was also required to limit heat transfer from the molten metal to the ladle, thus minimizing skull formation and protecting the ladle from melting or suffering a disastrous loss of strength. Another function of the coating was to permit those

skulls that did form to be easily and cleanly removed. A variety of materials were evaluated, including slips of kaolin and water glass, kyarite (Al_2SiO_5), and vermiculite, flame-sprayed alumina, and acetylene black. The dip coatings were preferred for their reliability and ease of application, but it was impossible to discriminate between them on the basis of performance. Selection should be based on price. Of the two types of metal ladles which were evaluated, the malleable iron ladles were preferred for their lower price.

Based on limited experience, it was concluded that clay graphite ladles would require no coating and could be used to transfer malleable iron with virtually no skull formation. They were much more fragile than metal ladles, however, posing a breakage problem.

There were several very persuasive reasons for wishing to integrate automatic molten metal transfer into the ferrous die casting process.

a. Ladle preparation and the associated consumable supplies, including ladles, could be eliminated

b. Handling losses in the form of ladle skulls could be eliminated; obviously, this would be particularly important when casting expensive alloys

c. The more promising devices for automatic transfer employ bottom pouring; therefore, a positive pressure of inert gas could be maintained over the melt, restricing oxidation losses, slag formation, and changes in melt composition and reducing the need for slag removal and melt maintenance

d. Transfer time and total cycle time could be reduced, and/or the operator would be able to simultaneously perform other tasks, e.g., trimming.

Of the three types of devices considered, those falling into the pneumatic category were preferred. The vacuum-lift technique of metal transfer was demonstrated to be technically feasible for ferrous die casting. An induction-heated transfer tube, constructed of concentric sleeves of alumina, graphite, and alumina, was demonstrated to be compatible with molten cast iron, and the induction heating maintained the tube at a sufficiently high temperature to prevent metal from solidifying on the walls or in the coupling between the tube and the shot sleeve. It was concluded that such a tube would be satisfactory for either a vacuum-lift or a hyperbaric pneumatic transfer device.

Water-model studies of the vacuum transfer system of automatic metal transfer indicated that the problem of spillage into the lower end of the shot sleeve was characteristic to the vertical orientation of the shot sleeve of a vertical die casting machine. The vacuum lift system of automatic transfer, therefore, was concluded to be better suited to horizontal die casting machines than to vertical die casting machines.

Based on a computer analysis and some supporting experimental evidence, it was concluded that the vacuums that can be readily achieved in a vacuum lift automatic transfer device are relatively poor. Although they are sufficiently low to achieve effective transfer, they are not sufficiently low to significantly reduce the incidence of porosity in the castings related to entrapped gas. Dies designed for use with such a system will require conventional venting.

3. PRESSURE INJECTION

It was concluded that if ferrous die casting were to depend on hand ladling, vertical die casting machines could be used only if their shot sleeves were insulated with an expendable shot cup. Asbestos cups proved to have satisfactory thermal properties for that assignment, but they had to be dried at 1800°F to reduce gas evolution to a tolerable level, and they proved to be a source of flake-like inclusions in the castings.

High-density, pressed and sintered molybdenum, AISI D5, and AISI H-13 were employed as shot sleeves for ferrous die casting on a vertical die casting machine. The D5 and the H-13 were hardened and nitrided. Only the H-13 performed satisfactorily. The molybdenum was concluded to be too soft for the application. The D5 was concluded to have insufficient resistance to thermal shock.

The ferrous die castings produced on vertical machines in this program contained no less entrapped gas than those produced on horizontal die casting machines. The only obvious advantage for vertical machines was their smaller floor space requirement. Vertical die casting machines were concluded to be unsatisfactory for ferrous die casting.

Large temperature differences were demonstrated from point to point in conventional shot sleeves used for ferrous die casting on horizontal die casting machines. Dimensional variations related to those temperature differences were concluded to be the chief obstacle

to the accomplishment of a long-lived, precision, pressure injection system. It was demonstrated that the temperature differences could be virtually eliminated by using a heated composite sleeve constructed from three concentric tubes, if the tube sandwiched between the other two was a good thermal conductor. The heat was supplied with equal success by an external induction coil and cartridge heaters sunk in the sandwiched tube.

Temperature differences were controlled even better in a two-piece shot sleeve, the outer tube of which was fabricated as a finned body from a good thermal conductor. Hot spots could be eliminated by the selective application of forced air.

Both the finned, air-cooled shot sleeve and the induction-heated shot sleeve, however, were too bulky to be readily employed on a 400 ton Lester die casting machine. It is anticipated that the same difficulty would be experienced with die casting machines produced by other manufacturers. On the basis of its general applicability, therefore, the composite shot sleeve heated by cartridge heaters was selected as the best of the three systems evaluated.

As a general rule, the design of pressure injection systems should be such that interfaces between concentric components are not exposed to molten metal. Pressure injection systems should also be designed in such a way that any thrust transmitted to the shot sleeve will be borne by the stationary platen, not the top clamping plate or the cover impression block.

AISI 4130, slip-cast Al_2O_3, Molex 7, wrought molybdenum, René 41, René 41 flame-sprayed with ZrO_2, and TZM coated with $MoSi_2$ were all evaluated as shot sleeve liners for ferrous die casting on a horizontal die casting machine. The uncoated Rene 41 performed better than any of the other metallic liners. It was concluded that the wall thickness of a Rene 41 liner should never be less than 1/4". In a very brief test, the shot sleeve liner slip cast from Al_2O_3 also looked extremely promising.

AISI 4130 and Molex 7 were unsatisfactory, due to plastic deformation. Wrought molybdenum resisted plastic deformation but did not have sufficient resistance to abrasion and wear. The flame-sprayed ZrO_2 spalled off from the Rene 41, and it was deduced that the same thing happened to the $MoSi_2$ coating on the TZM liner, leaving the inherent capabilities of that material unknown. Permanent protective coatings for shot sleeve liners appeared to have little promise.

Graphite-ceramics were investigated as shot sleeve liners and found to have insufficient resistance to abrasion and wear. Fiber-reinforced ceramics were considered and dismissed as being too costly. The use of BeO as a shot sleeve liner was disregarded because of its toxicity; the use of SiC was dismissed because it was known to react with molten steel. In retrospect, the conclusions concerning BeO and SiC should, perhaps, be re-examined.

Although slip-cast Al_2O_3, beryllium-copper, Nitralloy 135, and TZM were all used as plunger tips in the course of the project, beryllium-copper, which is probably the standard plunger material in the aluminum die casting industry, was used for the pilot casting operation. The frequency with which the beryllium-copper plungers required replacement made a more wear-resistant material desirable. H-13, which is stronger and harder than beryllium-copper, also has a lower coefficient of thermal expansion. It has good resistance to thermal shock, and it, too, has been used for aluminum die casting. It was concluded that H-13 plungers should be evaluated.

TZM and Invar were considered solely on the basis of their characteristically low thermal expansion. The Invar certainly has no better wear resistance than beryllium-copper. TZM was employed only briefly, but successfully, as a plunger in an Al_2O_3 shot sleeve liner. Its value in that application is uncertain. The slip-cast Al_2O_3 was so porous that it was infiltrated by molten iron. Although that problem could certainly be eliminated, and although Al_2O_3 is hard and abrasion resistant and has a low coefficient of thermal expansion, its ultimate value as a plunger is questionable. The problem of fastening an Al_2O_3 plunger to a plunger rod is most difficult, due to the low tensile strength and ductility of the material. The low strength and ductility might well adversely affect the durability of an Al_2O_3 plunger, too.

Process economics can be improved by the simple expedient of providing the die casting machine with automatic plunger lubrication.

If soldering occurs between the molten metal being cast and the dies, that soldering occurs during pressure injection. It was concluded that the following factors affect the probability of soldering:

a. High pouring temperatures

b. High metal injection pressures

c. High injection velocities

d. High mass flow through a given channel or past a given point

e. Turbulent flow

f. High carbon activities

g. Highly fluid melts

h. Inadequate die protection

i. Poor die lockup, i.e., flashing.

The following process parameters were concluded to be desirable for die casting malleable iron hemispheres in part to restrict soldering:

a. Pouring temperature: 2380°F to 2410°F (for a malleable iron with a carbon equivalent of 3.2% to 3.3%)

b. Injection pressure: 2850 pounds per square inch

c. Injection velocity: 50" per second (maximum).

4. DIE DESIGN

It was concluded that there are a number of general principles for the design of dies for ferrous die casting. These principles are enunciated below.

a. Die components that are exposed to molten metal should be made from refractory metals

b. For economic reasons, other die components should be fabricated from steel

c. The design must accommodate thermal expansion differences between refractory metals and steels, while maintaining firm lockup, registry across the parting line, and close proximity of separate refractory metal blocks in each die half

d. Internal threads must be avoided when attaching refractory metals

e. The number of interfaces between die components that are exposed to molten metal should be restricted; those interfaces that cannot be eliminated should incorporate the "safety groove" concept

f. Support pillars should be employed in the ejector half of large dies to avoid flexure (the reality of flexure was demonstrated by Doehler Jarvis' shimming experiment)

g. Dies require substantial cooling during operation and should be insulated from platens, tie bars, and the ejection mechanism of the die casting machine

h. Provision must be made to preheat the dies to their equilibrium operating temperature before putting them in service

i. The cross sections of the runners and gates should be relatively large, to affect rapid mass transfer at low metal velocities; this is felt to be beneficial in reducing soldering, heat checking, and gross cracking

j. Runners should be as short as possible, to improve yield and to reduce injection time and heat losses

k. Ejector pins should bear only on overflows, not runners and castings; not only does this produce a part of better appearance, but it prolongs die life; ejector pin holes are sources of crack initiation and can be distorted by shrinking castings

l. Steel ejector pins appear to be satisfactory

m. Draft angles should be made as generous as possible, to avoid plastic deformation of the die and/or hot tearing of the castings

n. For economic reasons, the number of cavities per die should be maximized

o. Gates should be designed with trimming techniques in mind

p. Die surfaces exposed to molten metal should be polished, to eliminate possible sites for crack initiation.

The specific design features that satisfy these principles are innumerable. Many examples are cited in the body of the report.

Sixteen different die materials were evaluated in the course of the program. They are listed below and on the following page, in order of decreasing preference.

a. Mo-3

b TZM

c. High-density, pressed and sintered molybdenum

d. HOT SHOT 2920X

e. Wrought molybdenum

f. Silicided wrought molybdenum

g. Cb-25% Zr

h. 80-20

i. Pressed and sintered tungsten

j. Anviloy 1150

k. Pressed and sintered tungsten-2% thoria

l. GE-474

m. Cb-752

n. Copper-infiltrated molybdenum

o. H-13

p. Copper-infiltrated tungsten.

Those materials ranked below Cb-25% Zr were concluded to be of little further interest for ferrous die casting. The siliciding treatment did not improve the performance of wrought molybdenum, and it was concluded to be of no particular value for ferrous die casting dies. Of the remaining materials, Mo-3 experienced pitting; TZM delaminated high-density, pressed and sintered molybdenum experienced plastic deformation and gross cracking; HOT SHOT 2920X exhibited heat checking; wrought molybdenum suffered delamination; and Cb-25% Zr experienced heat checking. Of these five materials, only the TZM, the high-density, pressed and sintered molybdenum, the wrought molybdenum, and the Cb-25% Zr are commercially available.

It was concluded that heat checking (i.e., thermal fatigue) was not the most important mode of failure in ferrous die casting dies. A minimum of 10% elongation in every direction was concluded to be a minimum acceptable standard for die materials. Stress relief annealing dies before putting them into service was concluded to be a wise precaution, but not a proven necessity. The performance of any die material was concluded to be related to its location in a die and the configuration of the die, regardless of the operating parameters (e.g., areas exposed to high metal velocities, or high mass flow, or large cavity cross sections will suffer distress before areas exposed to low metal velocities, or low mass flow, or small cavity cross sections).

TZM and high-density, pressed and sintered molybdenum were used in the pilot production die. Both performed better than they had in the Lamp Metals and Components Department materials evaluation die, leading to the conclusion that the lower pouring temperatures, lower injection pressures, and lower injection velocities had favorably affected die life. The resistance of the TZM to delamination was concluded to have been improved by the special forging practice employed, but the delamination problem had not been eliminated entirely. It was concluded that similar techniques would be beneficial for wrought molybdenum, which also exhibits a tendency to delaminate but has less tendency to become heat checked and pitted than TZM has. The high-density, pressed and sintered molybdenum was concluded not to have been a good selection for the ejector die, because first wear lands and later gross cracks were observed around the base of the hemispherical cores.

On the other hand, high-density, pressed and sintered molybdenum had demonstrated excellent resistance to pitting and heat checking in the materials evaluation die. The pitting and heat checking that were observed on the hemispherical cores of the pilot casting die, therefore, were accepted as an indication that improved gating and/or better cooling of the hemispherical cores was required. It was also concluded that the circumferential slot required through the fins of the hemispheres should be formed by cores in the die and that the sprue should have been the same diameter as the bore of the shot sleeve.

The pilot casting experience indicated that soldering was not inevitable. Therefore, it was concluded that the MoS_2 preparation used by Doehler Jarvis had been inadequate as a die release agent.

5. CASTING QUALITY

The castings produced in the course of this project were characterized by excellent surface finishes, good reproduction of detail, and little variance in weight and dimensions, but they were also characterized by porosity. Some of the porosity that was observed was spherical in shape, tended to float upward, was scattered and refined by slit gates (but not by larger gates), and was reduced by increased injection pressures. That porosity was related to entrapped gas. Some of the porosity was irregular in shape and was concentrated in the areas of the castings having the greatest cross section. That porosity was concluded to be related to solidification shrinkage, and it, in turn, was revealed to be responsible for hot tearing in the hemispheres.

It was concluded that high injection velocities and high injection pressures would reduce gas bubbles but would also menace die life. It was concluded that the shrinkage porosity concentrated in the flutes of the hemispheres could have been reduced through skillful die design.

All cast irons were demonstrated to be "white iron" as die cast, although very small spherulites of graphite were discovered to have nucleated in die cast nodular (ductile) iron. The conclusion must be that grey iron cannot be produced practically by the die casting process. Upon annealing, all of the cast irons but the ductile iron formed temper carbon nodules; the ductile iron formed spherulites. Eutectic iron (4.25% CE) could be graphitized (malleablized) in 15 minutes at 900°C. Attempts to malleablize eutectic iron in short times at subcritical temperatures which approximated the temperature of the casting at ejection (i.e., <723°C) were unsuccessful. The graphitization of 3.1% CE iron (with only 1.5% silicon) required more than four hours at 900°C. The relative ease with which malleable iron was made by the die casting process, compared to relative difficulty with which nodular iron was made, leads to the conclusion that nodular iron is not a practical product for the ferrous die casting process. The eutectic iron hemispheres die cast by Doehler Jarvis tended to break in the die, whereas 3.1% CE malleable iron hemispheres did not. It was concluded that it was not practical, in general, to die cast eutectic iron.

In general, die casting produces finer microstructures than other casting techniques. It may also produce nonequilibrium structures; both effects are particularly noticeable in thin sections. The 304 stainless steel die cast under this project may be one example of a non-equilibrium structure. The higher hardnesses found in thin sections were apparently reduced by self-annealing in heavy sections. A martensitic structure as die cast was exhibited by 403 stainless steel.

The alloyed 3.25% CE malleable iron produced in the pilot casting operation was concluded to have never completely graphitized, even after a prolonged batch annealing treatment. The alloyed malleable iron also developed a coarser structure with a larger ferritic grain size and a larger graphite nodule diameter in the thin, unfluted sections of the castings than the heavy fluted sections of the castings. The anomalous behaviour of the material was attributed to retained carbides, which also made the alloyed malleable iron very difficult to machine. It was concluded that the malleable iron from which the hemispheres were cast should not have been alloyed. Batch annealing did cause the hemispheres to become distorted, and it was concluded that they should have been given a continuous anneal and/or fixtured for annealing.

On the basis of Doehler Jarvis' experience, it was concluded that the tendency to solder was related to the carbon activity of the cast metal. The ferrous alloys die cast by Doehler are ranked here, in order of an increasing propensity to solder:

a. 304 stainless steel

b. 403 stainless steel

c. 3.1% CE malleable iron

d. Eutectic iron.

6. COST

Several conclusions specifically related to the cost of die casting malleable iron hemispheres are presented here. The hemispheres should be produced in an eight-cavity die which could be operated on a die casting machine having a lockup force of as little as 200 pounds. To produce 180 shots per hour, a minimum melting capacity of 775 kw and a combined furnace capacity of 1550 pounds will be required. The casting operation, including melting, casting, ladle preparation, material handling, trimming, inspection, and preventative maintenance, but excluding repair, housekeeping, chemical analysis, annealing, and cleaning, might require as many as ten men, two or three furnaces, and two trimming presses.

The process modifications required to reduce the shop cost of a pair of hemispheres to approximately \$.929 do not appear to be unduly difficult. Projections have been made which indicate that the shop cost might eventually be brought down to \$.195 per pair. That is an ambitious goal that depends, among other things, upon halving the first cost per cavity of the die, repairing the die only after every 20,000 cycles, achieving an ultimate die life of ten million hemispheres, and realizing a production rate of 180 cycles per hour, 24 hours per day, melting scrap only. Authorization to eliminate the circumferential slot that accommodates the locking band would greatly increase the probability of attaining that goal.

SECTION XI

RECOMMENDATIONS FOR FUTURE WORK

The ferrous die casting process remains confronted with a number of problems that have been only partially solved. Two unsolved problems related to the production of die cast ferrous hemispheres should be reviewed first. They are as follows.

a. Hot Tearing: This problem is associated with the solidification shrinkage concentrated in the flutes of the hemispheres. It has been suggested that the solution to this problem lies in die design.

b. Slot Formation: Casting circumferential slots into the hemisphere flutes is a challenge even in aluminum. A technique for malleable iron is required.

A number of more general problems have been identified that can also be recommended for additional work. They are as follows.

a. Gas Porosity: This is a problem that is process related, rather than part related. Until this problem is solved, the applications for the ferrous die casting process may be limited to those areas where mechanical properties are of secondary importance.

b. First Die Cost: New design concepts and new fabrication techniques may be required to reduce die cost significantly.

c. Die Repair: This rather prosaic problem may have as much impact on the economics of ferrous die casting as any other aspect of the process does.

d. Automatic Transfer: In addition to the other advantages of such a system, an early solution to this problem would relieve the process of the ten pound shot weight limitation imposed by human engineering factors.

e. Injection System: A more durable system is desired. In the near term, a refractory ceramic may be an answer. A workable hot chamber ferrous die casting process could eventually solve this problem and satisfy the requirement for an automatic transfer device as well.

f. Die Materials: This problem area encompasses both compositions and fabrication techniques. If the process must rely on wrought refractory metals, general principles must be established from which specific processing techniques can be derived. Alternatively, a pressed and sintered material or a wrought and recrystallized material must be discovered that possesses sufficient strength and ductility to be the basis for the next generation of die materials.

g. Die Cast Materials: The structure, properties, and response to heat treatment of die cast ferrous alloys have been demonstrated to be unpredictable. An examination in-depth of the characteristics of a number of the more important engineering alloys might prove to be most useful.

Perhaps the most fruitful way to solve many of the problems listed above would be to cope with then in actual production. Eventually, industry will do exactly that. That day can be hastened, however, by reducing the risk implicit in such a venture. What industry requires are process improvement/production contracts with the emphasis on production and reimbursement on a cost-plus incentive-fee basis.

APPENDIX I

DETAILS OF THE INVESTIGATION OF FERROUS DIE CASTING CONDUCTED BY DOEHLER JARVIS

22 April 1968--Hemisphere Die--Eutectic Iron

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating
1 (b)	2300	500	7	25	4,000	(a)
2 (b)	2300	500	7	25	4,000	(a)
3 (b)	2300	500	12	25	4,000	(a)
4 (c)	2300	500	10	7.5	4,000	(a)
5	2300	500	10	7.5	4,000	(a)
6	2300	500	10	7.5	4,000	(a)
7	2300	500	9	7.5	4,000	(a)
8	2300	500	8.5	7.5	4,000	(a)
9 (d)	2300	500	8	7.5	4,000	(a)
10	2300	500	8	7.5	4,000	(a)
11	2300	500	8	7.5	4,000	(a)
12	2300	500	8	7.5	4,000	(a)
13	2300	500	8.5	7.5	4,000	(a)
14	2300	500	8.5	7.5	4,000	(a)
15	2300	500	8.5	7.5	4,000	(a)

Comments

(a) Not radiographed
(b) Castings and gates broke during ejection--brittle
(c) The dwell time was reduced, affecting a reduction in breaking of the gates and cracking of the castings
(d) Hair-line crack developed in cover Impression 2 (pressed and sintered tungsten-2% thoria)

23 April 1968--Hemisphere Die--Eutectic Iron

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating
1	2300	500	7	7	4,000	(a)
2	2300	500	7	7	4,000	(a)
3	2300	500	7	7	4,000	(a)
4	2300	500	15	7	4,000	(a)
5	2300	500	15	7	4,000	(a)
6	2300	500	15	6	4,000	(a)
7	2300	500	15	6	4,000	(a)
8	2300	500	15	6	4,000	(a)
9	2300	500	15	6	4,000	(a)
10	2330	500	15	6	4,000	(a)
11	2330	500	15	5.2	4,000	(a)
12	2330	500	15	5.2	4,000	(a)
13	2330	500	16	5.2	4,000	(a)
14 (b)	2330	500	16	5.2	4,000	(a)
15	2330	500	16	5.2	4,000	(a)
16	2330	500	16	5.2	4,000	(a)
17	2330	500	16	5.2	4,000	(a)
18	2330	500	16	5.2	4,000	(a)
19	2330	500	16	5.2	4,000	(a)
20	2330	500	16	5.2	4,000	(a)
21	2330	500	16	5.2	4,000	(a)
22	2330	650	16	5.2	4,000	(a)
23	2300	650	16	5.2	4,000	(a)
24	2300	650	16	5.2	4,000	(a)
25	2300	650	16	5.2	4,000	(a)
26	2300	650	16	5.2	4,000	(a)
27	2300	650	16	5.2	4,000	(a)
28	2300	650	8	5.2	4,000	(a)
29	2300	650	8	5.2	4,000	(a)
30	2300	650	8	5.2	4,000	(a)
31	2300	650	8	5.2	4,000	(a)
32	2300	650	8	5.2	4,000	(a)
33	2300	650	8	5.2	4,000	(a)
34	2300	650	8	5.2	4,000	(a)
35	2330	650	8	5.2	4,000	(a)
36	2330	650			4,000	(a)

Comments

(a) Not radiographed

(b) Hair-line crack in cover Impression 2 increased; a casting welded to the tungsten-2% thoria; a small piece of the tungsten-2% thoria was removed; the impression became inoperative

25 April 1968--Hemisphere Die--Eutectic Iron

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating
1	2340	600	6	7.5	4,000	(a)
2	2340	600	6	7.5	4,000	(a)
3	2340	600	7.5	7.5	4,000	(a)
4	2340	600	8	7.5	4,000	(a)
5	2340	600	8	7.5	4,000	(a)
6	2340	600	8	7.5	4,000	(a)
7	2340	600	8	7.5	4,000	(a)
8	2340	600	8	7.5	4,000	(a)
9	2340	650	8	7.5	4,000	(a)
10	2340	650	8	7.5	4,000	(a)
11	2340	650	8	7.5	4,000	(a)
12	2340	650	8	7.5	4,000	(a)
13	2340	650	8	7.5	4,000	(a)

Comments

(a) Not radiographed

30 April 1968--Hemisphere Die--Malleable Iron+

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating
1	2600	600	7.5	8	4,000	(a)
2	2600	600	7.5	8	4,000	(a)
3	2600	600	7.5	8	4,000	(a)
4	2540	600	7.5	4	4,000	(a)
5	2540	600	7.5	4	4,000	(a)
6	2600	600	7.5	4	4,000	(a)
7	2600	600	8	4	4,000	(a)
8	2600	600	8	4	4,000	(a)
9	2600	600	9	4	4,000	(a)
10	2600	600	7.5	4	4,000	(a)
11	2600	600	7.5	4	4,000	(a)
12 (b)	2600	600	7.5	4	4,000	(c)

30 April 1968--Hemisphere Die--Malleable Iron+
(Continued)

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating
13 (d)	2600	600	7.5	4	4,000	(a)
14	2700	600	7.5	4	4,000	(a)
15 (e)	2700	600	7.5	4	4,000	(a)
16	2700	600	7.5	4	4,000	(a)
17	2700	600	7.5	4	4,000	(a)
18	2700	600	7.5	4	4,000	(a)
19	2700	600	7.5	4	4,000	(a)
20	2700	600	7.5	4	4,000	(a)
21	2700	600	7.5	4	4,000	(a)
22	2700	600	7.5	4	4,000	(a)
23	2680	600	7.5	4	4,000	(a)
24	2680	580	7.5	4	4,000	(a)
25	2680	580	7.5	4	4,000	(a)

Comments

The dies were coated with Glas-Mol and acetylene black. The ladles were stainless steel coated with WRP-X and a clay-graphite crucible. Both were heated to red heat in a gas furnace. More skull was left in the clay graphite crucible than in the WRP-X coated ladle.

+ 3.0% CE, i.e., 2.5% carbon, 1.5% silicon, 0.5% manganese
(a) Not radiographed
(b) Plunger stroke reduced; 2-1/8" high asbestos cup used
(c) Dispersed porosity near the in-gate; more pronounced in Impression 3
(d) Reheated asbestos cup in gas furnace
(e) After being held for 1-1/2 hours, the melt became very viscous; boiling occurred when filling the ladles; heavy skull formed in the ladles

1 May 1968--Hemisphere Die--Malleable Iron

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating
1	2600	625	7.5	3.1	4,000	(a)
2	2600	625	7.5	3.1	4,000	(a)
3	2600	625	7.5	3.1	4,000	(a)
4	2600	625	7.5	3.1	4,000	(a)
5	2600	625	7.5	3.1	4,000	(a)
6	2600	620	7.5	3.1	4,000	(a)
7 (b)	2600	620	7.5	3.1	4,000	(a)
8	2610	610	7.5	3.1	4,000	(a)
9	2630	610	7.5	3.1	4,000	(a)
10 (c)	2630	600	7.5	3.1	4,400	(a)
11	2630	575	7.5	3.1	4,400	(a)
12	2650	575	7.5	3.1	4,400	(a)
13	2650	575	7.5	3.1	4,400	(a)
14	2700	550	7.5	3.1	4,400	(a)
15	2700	550	7.5	3.1	4,400	(a)
16 (d)	2700	550	7.5	3.1	4,400	(a)
17	2710	535	7.5	3.1	4,400	(a)
18	2710	520	7.5	3.1	4,400	(a)
19	2710	520	7.5	3.1	4,400	(a)
20	2720	500	7.5	3.1	4,400	(a)
21	2720	520	7.5	3.1	4,400	(a)
22	2720	575	7.5	3.1	4,400	(a)
23	2720	600	7.5	3.1	4,400	(a)
24	2750	625	7.5	3.1	4,400	(a)
25	2780	640	7.5	3.1	4,400	(a)
26	2780	640	7.5	3.1	4,400	(a)
27	2780	640	7.5	3.1	4,400	(a)
28	2780	640	7.5	3.1	4,400	(a)
29	2780	645	7.5	3.1	4,400	(a)

Comments

An effort was made in this run to reduce the transfer time. It was reduced from 12 seconds to 7 seconds.

(a) Not radiographed
(b) Metal began to boil in ladle
(c) Hydraulic injection cylinder pressure increased to 1100 psi before this shot
(d) At this point, heavy skulls were formed over the lips of the ladles; 1/8" to 1/4" thick skulls remained in the bowl of the ladle after every shot

2 May 1968--Test Bar Die--Malleable Iron

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating
1	2640	600	10	4	4,400	(a)
2	2670	600	10	4	4,400	(a)
3	2670	600	10	4	4,400	(a)
4	2670	600	8	4	4,400	(a)
5	2670	620	8	4	4,400	(a)
6	2670	620	8	4	4,400	(a)
7	2650	620	8	4	4,400	(a)
8	2630	620	8	4	4,400	(a)
9	2630	620	8	4	4,400	(a)
10	2630	620	8	4	4,400	(a)
11	2630	620	8	4	4,400	(a)

Comments

For this run, the part of the die that forms the gripping section of the tensile bar was increased from 7/16" diameter to 9/16" diameter to repair a 3/32" by 3/4" pullout that resulted from soldering during a previous run; the in-gate was increased in thickness to 0.180" to avoid further soldering.

(a) Not radiographed

3 May 1968--Test Bar Die--3.1% CE Malleable Iron

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating
1	2650	500	5	3	4,400	(a)
2	2650	520	5	3	4,400	(a)
3	2650	520	5	3	4,400	(a)
4	2650	540	6	3	4,400	(a)
5	2660	560	6	3	4,400	(a)
6	2660	560	6	3	4,400	(a)
7	2660	560	6	3	4,400	(a)
8	2650	560	6	3	4,400	(a)

3 May 1968--Test Bar Die--3.1% CE Malleable Iron
(Continued)

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating
9	2650	560	6	3	4,400	(a)
10	2560	550	6	3	4,400	(a)
11	2560	550	6	3	4,400	(a)
12	2560	550	6	3	4,400	(a)
13	2560	550	6	3	4,400	(a)
14	2560	550	7.5	3	4,400	(a)
15 (b)	2580	540	7.5	3	4,400	(a)
16	2580	540	7.5	3	4,400	(a)
17	2580	540	7.5	3	4,400	(a)
18	2580	540	7.5	3	4,400	(a)
19	2580	540	7.5	3	4,400	(a)
20	2580	540	7	2.7	4,400	(a)
21	2600	500	7	3	4,400	(a)
22	2600	500	7	3	4,400	(a)
23	2600	500	7	3	4,400	(a)
24	2600	475	7	3	4,400	(a)
25	2640	450	7	5.8	4,400	(a)
26	2640	470	7	5.8	4,400	(a)
27	2640	475	7	5.8	4,400	(a)
28	2640	475	7	3	4,400	(a)
29	2640	475	7	3	4,400	(a)
30	2650	500	6	3.5	4,400	(a)
31	2650	500	6	3.5	4,400	(a)
32	2650	520	6	3.5	4,400	(a)
33	2650	520	6	3.5	4,400	(a)
34	2650	540	6	3.5	4,400	(a)
35	2620	540	6	3.5	4,400	(a)
36	2620	540	6.5	3.5	4,400	(a)
37	2620	540	6.5	3.5	4,400	(a)
38	2620	560	7	3.5	4,400	(a)
39	2620	560	7	3.5	4,400	(a)
40	2620	600	7.5	3.5	4,400	(a)
41	2620	600	7.5	3.5	4,400	(a)
42	2620	600	7.5	3.5	4,400	(a)
43	2620	600	7.5	3.5	4,400	(a)
44	2640	610	5	3.5	4,400	(a)
45	2640	610	5	3.5	4,400	(a)

3 May 1968--Test Bar Die--3.1% CE Malleable Iron
(Continued)

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating
46	2640	610	5	3.5	4,400	(a)
47	2640	610	6	4	4,400	(a)
48	2660	620	6	4	4,400	(a)
49	2660	620	6	4	4,400	(a)
50	2660	620	7	5.4	4,400	(a)
51	2660	620	7	5.4	4,400	(a)
52	2660	625	6	5.7	4,400	(a)

Comments

A high-density, pressed and sintered molybdenum shot sleeve was used; after the run, it was badly scored. The malleable iron gates and castings did not break in the die as did the eutectic iron gates; the complete gates, consisting of a tensile bar, an impact bar, runners, in-gates, and a biscuit, were ejected intact, and surface cracks were reduced.

(a) Not radiographed
(b) Approximately 100 pounds of grey iron were added to the melt before this shot; the cracking was reduced further

5 May 1968--Test Bar Die--Malleable Iron

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating* Tensile	Impact
1	2640	560	3	3.5	4,400	1	4
2	2640	580	5	3.5	4,400	1	4
3	2640	580	5	3.5	4,400	2	2
4	2680	580	5	3.5	4,400	1	3
5	2680	580	6	3.5	4,400	2	4
6 (b)	2680	590	10	3.5	4,400	2	4

Comments

*1: Least porous; 4: Most porous; Increasing injection velocities did not reduce porosity.

(b) The upper end of the relatively soft molybdenum sleeve became bell-mouthed, permitting an iron chip to become wedged between the plunger and the sleeve. The plunger seized in the sleeve; and when the gates were released after Shot 6, the cover die lifted slightly away from the stationary platen. The run had to be terminated.

16 May 1968--Test Bar Die--Malleable Iron

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating* Tensile	Impact
1	2610	650	6	3.5	4,400	2	4
2 (b)	2610	650	6	3.5	4,400	(a)	

Comments

*1: Least porous; 4: Most porous

(a) Not radiographed
(b) Casting stuck in cover die; a small piece of the high-density, pressed and sintered molybdenum die broke out

20 May 1968--Test Bar Die--Malleable Iron

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating* Tensile	Impact
1	2650	600	6.5	2.8	4,400	2	4
2	2650	600	6.5	2.8	4,400	(a)	
3	2650	600	6.5	2.8	4,400	(a)	
4	2650	600	6.5	2.8	4,400	(a)	
5	2650	600	6.5	2.8	4,400	(a)	
6	2650	600	6.5	2.8	4,400	(a)	
7	2650	600	6.5	2.8	4,400	(a)	
8	2650	610	6.5	2.5	4,400	2	4
9	2650	610	6.5	2.5	4,400	(a)	
10	2650	610	6.5	2.5	4,400	2	4
11	2650	610	6.5	2.5	4,400	(a)	
12	2650	610	6.5	2.5	4,400	(a)	
13	2600	610	5	2.5	4,800	2	4
14	2600	610	5	2.5	4,800	(a)	
15	2600	610	5	2.5	4,800	3	4
16	2600	605	5.5	2.5	4,800	(a)	
17	2600	605	6	2.5	4,800	2	4
18	2600	605	6	2.5	4,800	(a)	

20 May 1968--Test Bar Die--Malleable Iron
(Continued)

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating* Tensile	Impact
19	2600	605	6	2.5	4,800	3	4
20 (b)	2600	605	6	2.5	4,800	(a)	
21	2600	600	7.5	3.8	4,800	3	4
22	2600	600	7.5	3.8	4,800	2	4
23	2600	600	7.5	3.8	4,800	(a)	
24	2600	610	6.5	2.7	4,800	(a)	
25	2600	600	7.5	2.85	4,800	3	4
26	2600	610	7	2.85	4,800	3	4

Comments

A hardened and nitrided AISI H-13 steel shot sleeve was introduced for this run, replacing the molybdenum sleeve. The H-13 sleeve had a conical flange at its upper end that seated in a conical depression in the ejector die. Since the shot sleeve was centered by the ejector die, it could be loose-fitting, and thereby accommodate the thermal expansion difference between the H-13 sleeve and the molybdenum gate block. The locking force of the die maintained tight joints. (This is the "self-cleaning" design concept.)

*1: Least porous; 4: Most porous
(a) Not radiographed
(b) Reduced porosity in 9/16" diameter gripping section of tensile bar castings from Shot 20 on suggested that higher injection pressures be investigated

21 May 1968--Test Bar Die--Malleable Iron

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating* Tensile	Impact
1	2640	500	7	2.4	4,800	(a)	
2	2640	540	7	2.4	4,800	(a)	
3	2640	560	7	2.4	4,800	4	4
4	2640	600	7	2.4	4,800	(a)	
5	2640	600	7	2.4	4,800	(a)	

21 May 1968--Test Bar Die--Malleable Iron
(Continued)

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating* Tensile	Impact
6	2640	610	7	2.4	4,800	4	3
7	2640	610	7	2.4	4,800	(a)	
8	2640	610	7	2.4	4,800	(a)	
9	2640	610	7	2.4	4,800	(a)	
10	2645	615	7.5	2.4	4,800	4	4
11	2660	615	7.5	2.4	4,800	(a)	
12	2660	615	7.5	2.4	4,800	(a)	
13	2660	615	7.5	2.4	4,800	(a)	
14 (b)	2660	615	7.5	2.4	4,800	4	4
15	2660	615	7.5	2.4	4,800	(a)	
16	2660	610	7.5	2.4	4,800	(a)	
17	2630	600	7.5	2.4	4,800	3	3
18	2600	580	7.5	2.4	4,800	3	3
19	2600	580	7.5	2.4	4,800	(a)	
20	2600	580	7.5	2.4	4,800	(a)	
21	2620	580	7.5	2.4	4,800	(a)	
22	2620	600	7.5	2.4	4,800	(a)	
23	2620	600	7.5	2.4	4,800	(a)	
24	2620	600	7.5	2.4	4,800	(a)	
25	2630	610	7	2.4	4,800	(a)	
26	2630	610	7	2.4	4,800	(a)	
27	2630	610	7	2.4	4,800	(a)	
28 (c)	2640	615	7	2.4	4,800	(a)	
29	2640	615	7	2.4	4,800	(a)	
30	2640	615	7	2.4	4,800	(a)	
31	2640	615	7	2.4	4,800	(a)	
32	2640	615	7	2.4	4,800	(a)	
33	2640	615	7	2.4	4,800	3	4
34	2645	605	7	2.4	4,800	3	4
35	2645	605	7	2.4	4,800	3	4
36	2650	595	7	2.4	4,800	3	4
37	2650	595	10	2.4	4,800	1	4
38 (d)	2650	595	10	2.4	4,800	3	4
39 (e)	2650	595	10	2.4	6,000	4	4
40 (f)							
41 (g)	2650	595	10	2.4	6,000	1	4
42	2640	595	9	2.4	6,000	(a)	
43	2640	595	9	2.4	6,000	(a)	
44	2640	595	9	2.4	6,000	(a)	
45 (g)	2610	600	9	2.4	6,000	1	4

21 May 1968--Test Bar Die--Malleable Iron
(Continued)

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-Ray Rating* Tensile	X-Ray Rating* Impact
46	2610	600	9	2.4	8,000	(a)	
47	2610	600	9	2.4	8,000	1	4
48	2610	600	9	2.4	8,000	(a)	
49	2610	600	9	2.4	8,000	(a)	
50	2605	600	10	2.4	8,000	(a)	
51	2605	615	10	2.4	8,000	4	4
52	2605	615	10	2.4	8,000	(a)	
53	2605	615	10	2.4	8,000	(a)	
54	2605	615	10	2.4	8,000	2	4
55	2600	630	11	2.4	10,000	3	4
56	2600	630	11	2.4	10,000	(a)	
57	2600	630	11	2.4	10,000	(a)	
58	2595	625	11	2.4	10,000	1	3
59	2595	625	11	2.4	10,000	(a)	
60	2590	620	11	2.4	10,000	1	2
61 (h)	2590	620	7.5	2.4	16,000	1	3
62	2590	620	7.5	2.4	16,000	(a)	
63	2590	620	7.5	2.4	16,000	(a)	
64	2590	625	8	2.4	16,000	1	3
65	2590	620	10	2.4	16,000	2	3
66	2590	620	10	2.4	24,000	(a)	
67 (i)	2590	620	10	2.4	24,000	1	4
68	2590	630	9.5	2.4	24,000	1	4
69	2590	640	9	2.4	24,000	1	4
70	2590	640	9	2.4	24,000	1	3

Comments

*1: Least porous; 4: Most porous

(a) Not radiographed
(b) Delayed shot
(c) After Shot 28, several more small pieces of molybdenum were observed to have broken out of the gate runner in the ejector die
(d) Radiography indicated that the porosity was, essentially, identical to that observed in the previous run
(e) Coincident with the increase in injection pressure, soldering again caused a piece of molybdenum to break out of the ejector die
(f) Oscillograph inoperative

Comments (Continued)

(g) Porosity reduced in 9/16" diameter gripping section of the tensile bar; no improvement noted in the impact bar

(h) Hydraulic line pressure reduced to 1000 psi and 1:4 pressure intensifier activated

(i) Radiographs of Shots 67 through 70 indicated that the 24,000 psi injection pressure affected a definite reduction of porosity; Visicorder indicated that the peak pressure was reached 0.4 seconds after the plunger motion ceased, perhaps too late to realize the full benefit of the high pressure on the solidifying castings

24 May 1968--Test Bar Die--Malleable Iron+/Aluminum

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating* Tensile	X-ray Rating* Impact
1 (b)	1220	600	7	3.2	17,600	(a)	
2 (b)	1220	600	7	3.2	17,600	(a)	
3 (b)	1220	600	7	3.2	17,600	(a)	
4 (b)	1220	600	7	3.2	17,600	(c)	
5 (d)	2630	600	25	3.3	17,600	2	2
6	2630	600	25	3.3	17,600	1	2
7 (e)	2630	600	2.5	3.3	17,600		
8	2630	600	2.5	3.3	17,600	1	
9	2630	600	2.5	3.3	17,600		
10	2630	600	2.5	3.3	17,600		
11	2630	600	2.5	3.3	17,600	1	
12	2640	620	8	3.3	24,000		
13	2640	620	8	3.3	24,000	1	
14	2640	620	8	3.3	24,000	3	3
15	2640	630	8	3.3	24,000		
16	2640	640	8	3.3	24,000	1	3
17	2640	640	8	3.3	24,000		
18	2640	640	8	3.3	24,000	1	3
19	2600	650	8	3.3	24,000		
20 (f)	2600	650	8	3.3	24,000	2	3
21	2600	650	8	3.4	24,000	1	3
22	2600	650	8	3.4	24,000		
23 (g)	2600	630	7.5	3.4	24,000		
24	2600	550	9.5	3.4	32,000	1	2
25	2600	550	9.5	3.4	32,000		

24 May 1968--Test Bar Die--Malleable Iron+/Aluminum
(Continued)

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating* Tensile	X-ray Rating* Impact
26	2600	550	9.5	3.4	32,000		
27	2600	600	9.5	3.4	32,000	1	2
28	2600	550	9.5	3.4	32,000		
29	2600	550	9.5	3.4	32,000		
30	2600	550	9.5	3.4	32,000	2	2
31	2600	610	8	3.4	32,000		
32	2600	610	9	3.4	32,000		
33 (b)	1220	570	9	3.4	32,000		

Comments

The higher injection pressures and velocities investigated increased the tendency to solder; small fragments of molybdenum pulled out of the die, making ejection and removal of the gate more difficult.

+3.5% CE, i.e., 2.5% carbon, 1.5% silicon, 0.5% manganese

*1: Least porous; 4: Most porous

(a) Not radiographed
(b) Shots 1 through 4 and 33 were made from aluminum
(c) X-ray photographs of Shot 4, an aluminum gate, revealed porosity similar to the first malleable iron gate, Shot 5
(d) Shots 5 and 6 were accidentally made at high plunger velocity, i.e., 25 ips, because the shot valve failed to reset; the high injection velocity resulted in finer, more dispersed porosity in the overflows
(e) For Shots 7 through 13, the impact bar cavity was blocked off; no appreciable difference in the porosity was noted
(f) Runner soldered in cover die
(g) Shot 23 was delayed; the mushroom-shaped tip of the shot sleeve became soldered

27 May 1968--Test Bar Die--2.48% CE Malleable Iron+

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating* Tensile	X-ray Rating* Impact
1	2660	600	2	2.5	17,600	1	2
2	2660	600	2	2.5	17,600	(a)	
3	2660	610	3	2.5	17,600	(a)	
4	2660	620	4	2.5	17,600	1	2
5	2660	620	4	2.5	17,600	(a)	
6	2660	620	4	2.5	17,600	(a)	
7	2660	620	4	2.5	17,600	(a)	
8	2670	630	4.5	2.5	17,600	3	2
9	2670	630	4.5	2.5	17,600	(a)	
10	2670	630	4.5	2.5	17,600	(a)	
11	2670	630	4.5	2.5	17,600	(a)	
12	2670	660	4.5	3	17,600	1	2
13	2670	675	7.5	3	17,600	(a)	
14	2680	690	7.5	3	17,600	(a)	
15	2680	690	7.5	3	17,600	3	4
16	2680	690	7.5	3	17,600	(a)	
17	2680	690	7.5	3	17,600	(a)	
18	2680	700	8	3	17,600	1	2
19	2700	690	11	3	17,600	(a)	
20 (b)	2680	650	12.5	3	17,600	(a)	
21	2680	670	14.5	3	17,600	2	2
22	2680	670	15	3	17,600	(a)	
23	2700	670	15	3	17,600	1	2
24	2700	670	15	3	17,600	(a)	
25	2700	670	15	3	17,600	3	2
26 (c)	2700	670	20	3	17,600	1	1
27	2700	670	20	3	17,600	(a)	
28	2700	670	20	3	17,600	1	2
29	2700	670	20	3	17,600	(a)	
30	2700	670	20	3	17,600	1	1
31	2700	670	18	3	17,600	1	1

Comments

This run investigated increasing injection velocities at a constant injection pressure of 17,600 psi; the increased injection velocities increased the tendency to solder.

+The carbon equivalent was measured with a Tectip, a thermal arrest device produced by Leeds and Northrup, Philadelphia, Pennsylvania

*1: Least porous; 4: Most porous

Comments (Continued)

(a) Not radiographed
(b) The carbon equivalent had fallen to 2.46% by Shot 20
(c) A slight improvement in the soundness of the castings was noted for Shots 26 through 31, made at plunger velocities of 18 and 20 ips, confirming aluminum die casting experience; the most obvious area of improvement seemed to be a reduction in the average pore size

28 May 1968--Test Bar Die--Malleable Iron+

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating* Tensile	X-ray Rating* Impact
1	2640	770	6.5	3.8	17,600	1	3
2	2640	770	6.5	3.8	17,600	(a)	
3	2640	770	6.5	3.8	17,600	3	3
4	2640	770	6.5	3.8	17,600	(a)	
5 (b)	2640	770	6	3.8	17,600	1	3
6	2630	790	6	3.8	17,600	(a)	
7	2610	820	6.5	3.8	17,600	1	2
8	2610	815	6.5	3.9	17,600	1	3
9	2610	820	6.5	3.9	17,600	(a)	
10	2610	820	6.5	3.9	17,600	1	2
11	2620	820	6.5	4.0	17,600	(a)	
12	2620	820	6.5	4.5	17,600	3	2
13	2620	820	6.5	4.5	17,600	(a)	
14 (c)	2640	800	6.5	4.5	17,600	1	2
15	2640	800	6.5	4.5	17,600	(a)	
16	2640	760	6.5	4.5	17,600	1	2
17 (d)	2640	800	6.5	4.5	17,600	(a)	
18	2690	800	14	4.5	17,600	2	2
19	2690	800	14	4.5	17,600	(a)	
20	2680	750	15	4.5	17,600	2	2
21	2680	730	14	4.5	17,600	(a)	
22	2700	730	12.5	4.5	17,600	1	2
23	2700	720	12.5	4.5	17,600	1	2
24	2700	730	12.5	4.5	17,600	(a)	
25	2700	730	12.5	4.5	17,600	(a)	
26	2704	750	12.5	4.5	17,600	(a)	
27	2700	760	11	4.5	17,600	1	2
28	2700	770	12	4.5	17,600	(a)	
29 (e)	2700	780	12.5	4.5	17,600	1	2

Comments

This run investigated the effect of higher die temperatures at various injection velocities at a constant injection pressure of 17,600 psi. The increased die temperatures at low plunger velocity did not reduce porosity; increasing the plunger speed from 6.5 ips to 14 ips did reduce porosity, however. The die was coated with Glas-Mol 7 and acetylene black; the ejector pins were lubricated with an oil suspension of graphite.

+CE = 3.07%

*1: Least porous; 4: Most porous

(a) Not radiographed
(b) Die recoated with Glas-Mol and soot after Shot 5
(c) Biscuit stuck in ejector die
(d) After Shot 17, soldering was observed at the vent end of the tensile bar cavity in that portion that forms the gripping section and in the ejector half of the runners to both the tensile and impact bars; small pieces of molybdenum broke out of both runners; Glas-Mol was reapplied after every six shots, and soot was reapplied after every shot. No further soldering was observed until Shot 29; at the time Shot 17 was made, the induction generator overheated, due to a water line obstruction
(e) The tensile bar runner again stuck in the ejector die after Shot 29; pitting of the high-density, pressed and sintered molybdenum was noted in the ejector die; the CE had fallen to 2.72%; the die was removed to be repolished and to have the in-gates enlarged

4 June 1968--Test Bar Die--Malleable Iron+

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating* Tensile	Impact
1	2610	600	4	4.1	17,600	(a)	
2	2610	680	5	4.1	17,600	(a)	
3	2610	680	5	4.1	17,600	1	
4	2610	680	5	4.1	17,600	(a)	
5	2610	660	5	4.1	17,600		2
6 (b)	2610	670	5	4.1	17,600	1	1
7 (c)	2610	670	5	4.1	17,600	(a)	
8	2610	650	4	4.1	17,600	(a)	
9	2610	650	5	4.1	17,600	(a)	

4 June 1968--Test Bar Die--Malleable Iron+
(Continued)

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating* Tensile	Impact
10	2610	650	4	4.1	17,600	1	2
11	2610	640	4	4.1	17,600	1	2
12	2680	680	3	4.1	17,600	2	2
13 (d)	2680	680	5	4.1	17,600	1	2
14	2675	685	5	4.1	17,600	1	2
15 (e)	2675	600	2.5	4.1	17,600	(a)	
16	2675	590	5	4.1	17,600	1	
17	2675	600	4	4.1	17,600	1	

Comments

For this run, the in-gates were increased to 0.300" thickness from 0.180"; side overflows were added, and the die was repolished. The die was coated with Glas-Mol and soot.

+CE = 3.05%

*1: Least porous; 4: Most porous

(a) Not radiographed
(b) Parts hanging on ejector pins
(c) Biscuit exploded; impact bar stuck in ejector die and broke on ejection
(d) The runners broke off the biscuit, and particles of molybdenum were pulled out of the ejector die when the gate was ejected
(e) For the last three shots, the runners had to be chisled out as the pitting of the ejector die increased

6 June 1968--Test Bar Die--Malleable Iron+

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating* Tensile	Impact
1	2650	680	3	3.9	2,800	(a)	
2	2650	680	4	3.9	2,800	(a)	
3	2670	660	3.5	4	2,800	(a)	
4	2670	660	3.5	4	2,800	2	-
5	2670	660	4	3.9	2,800	(a)	
6	2670	660	4	4	2,800	1	3

6 June 1968--Test Bar Die--Malleable Iron+
(Continued)

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating* Tensile	Impact
7 (b)	2670	680	5	4	2,800	(a)	
8	2670	680	5	4	2,800	(a)	
9 (b)	2670	680	5	4	2,800	(a)	
10	2670	680	6	4	2,800	(a)	
11 (c)	2670	680	6	4	2,800	(a)	
12	2670	680	6	4	2,800	(a)	
13	2670	680	5.5	4	2,800	(a)	
14	2670	680	6	4	2,800	2	3
15	2660	680	5.5	4	2,800	3	3
16 (d)	2660	680	6	4	2,800	(a)	
17	2660	680	5	4	2,800	(a)	
18 (e)	2660	680	5.5	4	2,800	(a)	
19	2660	680	6	4	2,800	(a)	
20 (f)	2660	680	6	4	2,800	(a)	
21	2680	620	6	4	2,800	(a)	
22	2640	640	7.5	4	2,800	(a)	
23	2640	640	7.5	4	2,800	3	3
24	2630	640	8.5	4	2,800	1	3
25 (g)	2630	640	9	4	2,800	(a)	
26	2610	590	5	4	2,800	(a)	
27	2610	600	8	4	2,800	(a)	
28 (h)	2610	610	9	4	2,800	(a)	
29	2610	610	9	4	2,800	(a)	
30	2610	615	9.5	4	2,800	2	3
31	2610	640	9.5	4	2,800	2	3
32	2610	640	10	4	2,800	(a)	
33	2610	650	10	4	2,800	(a)	
34	2610	650	10	4	2,800	(a)	
35	2610	650	12	4	2,800	(a)	
36	2610	630	10	4	2,800	(a)	
37	2620	640	11.5	4	2,800	(a)	
38	2620	650	11.5	4	2,800	(a)	
39	2620	660	12.5	4	2,800	4	4
40	2620	660	15	4	2,800	3	4
41	2620	660	15	4	2,800	(a)	
42	2620	640	13	4	2,800	(a)	
43	2620	640	15	4	2,800	(a)	
44	2620	650	15	4	2,800	(a)	
45	2620	650	15	4	2,800	(a)	
46 (i)	2620	600	5	4	4,400	(a)	

6 June 1968--Test Bar Die--Malleable Iron+
(Continued)

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating* Tensile	X-ray Rating* Impact
47	2680	600	5	4	4,400	(a)	
48	2680	600	6	4	4,400	(a)	
49	2670	630	5	4	4,400	(a)	
50	2670	625	5.5	4	4,400	1	3
51	2660	630	6	4	4,400	(a)	
52	2660	630	6	4	4,400	(a)	
53		650	5.5	4	4,400	1	3
54		650	6	4	4,400	(a)	
55		640	5	4	4,400	(a)	
56	2620	650	6	4	4,400	(a)	
57	2630	650	6	4	4,400	(a)	
58	2630	650	6	4	4,400	(a)	
59	2615	650	7	4	6,000	(a)	
60	2615	670	7	4	6,000	(a)	
61	2615	670	7	4	6,000	(a)	
62	2650	670	7	4	6,000	1	3
63	2650	680	6	4	6,000	2	3
64	2650	680	7	4	6,000	(a)	
65	2650	680	6.5	4	6,000	(a)	
66	2590	700	6.5	4	6,000	(a)	
67 (j)	2590	700	6.5	4	6,000	(a)	

Comments

For this run, the die was repolished to remove the cracks and pitting in the ejector die; relatively low injection pressures were employed.

+CE = 2.82%

*1: Least porous; 4: Most porous

(a) Not radiographed
(b) After Shots 7 and 9, the gates hung on the ejector pins
(c) After Shot 11, the impact bar runner broke off the biscuit during ejection
(d) After Shot 16, the tensile bar runner broke off the biscuit during ejection
(e) After Shot 18, the gate hung on the runner ejector pins

Comments (Continued)

(f) Shot 20 did not fill the overflows; the impact bar broke at the third ejector pin from the gate
(g) CE rechecked after Shot 25: CE = 3.15%
(h) From Shot 28 to 45, most of the runners broke off the biscuit during ejection; the tensile bar runner in the ejector die opposite the tip of the "self-cleaning" shot sleeve deteriorated seriously; small particles of molybdenum continued to be torn out of the die
(i) From Shot 46 on, the injection pressure was increased to 4,400 psi and the plunger speed was reduced to 5 to 7 ips, to avoid soldering but still enable the die to be filled
(j) CE rechecked at the end of the run: CE = 3.165%

7 June 1968--Test Bar Die--AISI 304 Stainless Steel

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating* Tensile	X-ray Rating* Impact
1	2920	670	3	4	4,400	(a)	
2	2920	670	3	4	4,400	(a)	
3	2920	670	3	4	4,400	(a)	
4	2920	670	3	4	4,400	(a)	
5	2920	680	3	4	4,400	1	2
6	2920	690	6	4	4,400	1	2
7	2920	700	5	4	4,400	(a)	
8	2920	700	5	4	4,400	(a)	
9	2920	700	5	4	4,400	(a)	
10	2920	700	5	4	4,400	(a)	
11	2910	675	3	4	17,600	(a)	
12	2800	660	8	4	17,600	(a)	
13	2930	640	6	4	17,600	(a)	
14	2980	640	7.5	4	17,600	(a)	
15	2980	640	6	4	17,600	1	2
16	2980	640	7.5	4	17,600	1	2
17	2900	635	11	4	4,400	1	2
18 (b)	2900	635	11	4	4,400	(a)	
19 (c)	2900	640	11.5	4	4,400	4	4
20 (d)	2900	640	12	4	6,000	1	1
21 (e)	3070	640	12	4	6,000	(a)	

Comments

This was the first run made with 304 stainless steel. The brightness temperature of the melt was noticeably greater than that of malleable iron. The melt was observed to smoke and form a heavy slag crust.

*1: Least porous; 4: Most porous

(a) Not radiographed
(b) The castings from Shot 18 hung on the ejector pins
(c) Shot 19 produced a short biscuit; the slag on the melt was becoming very heavy
(d) The castings from Shot 20 again hung on the ejector pins
(e) The castings from Shot 21 soldered in the cover die; no major deterioration was noted in the runner area during this run, however; casting was terminated because of operator eye fatigue

10 June 1968--Test Bar Die--AISI 403 Stainless Steel

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating* Tensile	X-ray Rating* Impact
1	3050	610	4	3.9	4,400	(a)	
2	2970	580	4	3.9	4,400	(a)	
3	2970	610	7	3.9	4,400	(a)	
4	2950	630	6	3.9	4,400	(a)	
5	2950	650	7.5	3.9	4,400	(a)	
6	2930	600	8	3.9	4,400	1	3
7	2930	610	7	3.9	4,400	(a)	
8	2930	620	8	3.9	4,400	(a)	
9	2900	600	13	3.9	4,400	(a)	
10	2900	620	12.5	3.9	4,400	(a)	
11	2930	620	13	3.9	4,400	1	3
12	2930	625	12.5	3.9	17,600	1	3
13	2930	630	12.5	3.9	17,600	(a)	
14	2960	630	12.5	3.9	17,600	1	2
15	2960	630	12.5	3.9	17,600	(a)	
16	2920	610	14	3.9	6,000	(a)	
17	2920	610	12.5	3.9	6,000	(a)	
18 (b)	2945	630	13	3.9	6,000	(a)	
19	2945	625	13	3.9	6,000	2	3

10 June 1968--Test Bar Die--AISI 403 Stainless Steel
(Continued)

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Rating* Tensile	X-ray Rating* Impact
20	2940	630	13	3.9	6,000	(a)	
21	2930	600	16	3.9	6,000	2	3
22	2930	620	16	3.9	6,000	2	3
23	2950	630	16	3.9	6,000	(a)	
24	2920	640	17.5	3.9	6,000	2	3
25	2900	650	11	3.9	6,000	1	2
26	2900	680	12.5	3.9	6,000	(a)	
27	2900	705	12.5	3.9	6,000	(a)	
28	2900	710	12.5	3.9	6,000	(a)	
29	2900	660	12.5	3.9	6,000	1	2
30	2900	670	12	3.9	6,000	(a)	
31	2900	680	13	3.9	6,000	(a)	
32	2900	690	13	3.9	6,000	(a)	
33	2900	690	13	3.9	6,000	1	2
34	2900	695	13	3.9	6,000	(a)	
35	2900	700	13	3.9	6,000	(a)	
36	2900	705	13	3.9	6,000	2	2
37	2900	630	11.5	3.9	6,000	(a)	
38	2900	650	--	3.9	6,000	(a)	
39	2900	670	14	3.9	6,000	1	1
40	2900	680	14	3.9	6,000	(a)	
41	2900	685	12.5	3.9	6,000	1	1
42	2900	670	13	3.9	6,000	(a)	
43	2900	680	14	3.9	6,000	1	1
44	2890	680	14	3.9	6,000	1	1

Comments

This was the first run with 403 stainless steel. It formed a heavy slag crust in the crucible and heavy skulls in the ladles.

*1: Least porous; 4: Most porous

(a) Not radiographed
(b) The tensile bar from Shot 18 soldered in die and broke during ejection; it was removed easily
(c) This run, too, was terminated because of operator eye fatigue; ejection was easy throughout the run, and no major breakout of die material occurred

20 June 1968--Hemisphere Die+--403 Stainless Steel

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Impression No.* 1	2	3
1	2910	580	5	4	24,000	4		3
2 (b)	2920	580	3	4	24,000	2		3
3 (c)	2950	580	3	4	24,000	2		3
4 (d)	3000	505	5	4	24,000	2		2
5 (e)	3060	530	6	4	24,000	2		2

Comments

+The hemisphere die, with the center impression blocked off, was replaced in the die casting machine as a two-impression die.

*1: Least porous; 4: Most porous

(b) The runner welded to the mushroom-shaped head of the shot sleeve, broke, and pulled out some steel particles from the shot sleeve head
(c) The gate hung in the ejector die and had to be chiseled out
(d) The gate again soldered in the ejector die and had to be chiseled out
(e) A 4" long crack developed in the D5 shot sleeve, and the runner welded again to the muchroom-shaped head of the shot sleeve; casting was discontinued and the shot sleeve removed

24 June 1968--Hemisphere Die+--403 Stainless Steel

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Impression No.* 1	2	3
1 (b)	3160	685	7	4	4,600	3		4

Comments

+Two impressions only

*1: Least porous; 4: Most porous

(b) The biscuit welded to the ejector die in the area opposite the plunger; it had to be chiseled out; the die had to be reground

25 June 1968--Hemisphere Die+--403 Stainless Steel

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Impression No.* 1	2	3
1	2960	425	4.5	4	2,800		(a)	
2	2960	420	4.5	4	2,800	1		1
3	2960	420	6	4	2,800		(a)	
4	2960	405	4	4	2,800	2		2
5	2950	405	4	4	2,800		(a)	
6	2950	405	4.5	4	2,800	2		2
7	3000	405	4.5	4	3,600		(a)	
8	3000	410	4.5	4	3,600	2		2
9	3000	400	9	4	3,600		(a)	
10	2965	405	9	4	3,600	3		4
11	2960	405	9	4	3,600		(a)	
12	2960	410	9.5	4	3,600	3		3
13 (b)	2920	385	9.5	4	3,600		(a)	

Comments

Before each shot, the die was coated with Glas-Mol and soot. In addition, the head of the shot sleeve, the area of the ejector die opposite the plunger, and the runners were coated with Arco Perm 100 prior to each shot. No ejection difficulties were experienced until Shot 13.

+Two impression only

*1: Least porous; 4: Most porous

(a) Not radiographed
(b) The runner soldered to the head of the shot sleeve at Shot 13

26 June 1968--Hemisphere Die+--304 Stainless Steel

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Impression No.* 1	2	3
1	2850	580	6	5	3,500	2		2
2	2850	590	6	5	3,500		(a)	
3	2910	595	7	5	3,500		(a)	
4	2910	600	7	5	3,500		(a)	
5	2910	600	7	5	3,500	2		2
6	2950	600	7	5	3,500		(a)	
7	2950	600	7	5	3,500		(a)	
8	2950	600	7	5	3,500		(a)	
9	2960	590	7	5	3,500		(a)	
10	2960	590	7	5	3,500		(a)	
11 (b)	2960	590	8	5	3,500	2		2
12	3080	560	3.5	5	3,500		(a)	
13 (c)	3060	560	3.5	5	3,500		(a)	
14	3040	580	5	5	3,500	2		2
15 (d)	3040	580	5	5	3,500		(a)	
16	3040	580	5	5	3,500		(a)	
17	3000	595	5	5	3,500	2		2
18	2950	600	5	5	3,500		(a)	
19	2950	600	5	5	3,600		(a)	
20	2950	600	5	5	3,600		(a)	
21	2950	600	5	5	3,600	2		1

Comments

The die was coated with Glas-Mol and soot before every shot. Before every shot, Arco Perm 100 was also applied to the runner areas and the ejector die.

+Two impressions only

*1: Least porous; 4: Most porous

(a) Not radiographed
(b) The biscuit exploded; there was some soldering in the shot sleeve that had to be ground away
(c) Impression 1 did not fill out
(d) The runners soldered to the mushroom-shaped head of the shot sleeve; a heavy slag crust began to form on the melt and heavy, difficult-to-remove slag skulls began to form on the ladle; these two conditions eventually forced the run to be terminated

27 June 1968--Hemisphere Die+--304 Stainless Steel

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Impression No.* 1	2	3
1	2740	525	4	4	3,400	2		2
2	2740	540	4	4	3,400		(a)	
3	2840	560	4	4	3,400		(a)	
4	2940	580	4	4	3,400	1		1
5	2960	590	4	4	3,400	1		1
6	2960	590	4	4	3,400	2		
7	2960	600	4	4	3,400	1		1
8	2960	610	4	4	3,400	1		2
9	2960	620	4	4	3,400	3		2
10	2960	625	4	4	3,400	1		2
11	2965	630	4	4	3,400		(a)	
12	2965	630	4	4	3,400	1		2
13	2965	620	4	4	3,400		(a)	
14	3030	605	4	4	3,400		(a)	
15 (b)	3030	600	4	4	3,400		(a)	
16	2920	520	4	4	3,400		(a)	
17	2930	530	4	4	3,400		(a)	
18	2920	550	4	4	3,400		(a)	
19	2920	560	4	4	3,400	3		2
20	2930	560	4	4	3,400		(a)	
21	2930	555	4	4	3,400		(a)	
22	2960	550	4	4	3,400		(a)	
23	2960	550	4	4	3,400		(a)	
24	2965	540	4	4	3,400		(a)	
25 (c)	2960	530	4	4	3,400	2		2
26	3100	520	4	4	3,500		(a)	
27	2960	530	4	4	3,500	2		3
28	2960	540	4	4	3,500		(a)	
29	2960	530	4	4	3,500		(a)	
30	3100	530	4	4	3,500		(a)	

Comments

+Two impressions only

*1: Least porous; 4: Most porous

Comments (Continued)

(a) Not radiographed
(b) After Shot 15, the protruding lip forming the interlocking channel in the castings produced in Impression 1 was observed to be cracked in two places, 1" apart, to the depth of the lip, interferring slightly with ejection (The die material was high-density, pressed and sintered molybdenum)
(c) Heavy slag was again encountered on the surface of the melt and as skulls on the ladles

12 July 1968--Hemisphere Die+--304 Stainless Steel

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Impression No.* 1	2	3
1	2950	700	1	4.2	3,600		(a)	
2 (b)	2950	705	1.5	4.2	3,600		(a)	
3 (c)	2960	710	2	4.2	3,600		(a)	
4	2960	710	2	4.2	3,600	2		2
5 (d)	2960	710	2	4.2	3,600		(a)	
6	2960	700	2.5	4.2	3,600		(a)	
7 (e)	2960	695	3	4.2	3,600		(a)	
8	2960	700	3	4.2	3,600		(a)	
9	2960	700	3	4.2	3,600	2		1
10 (f)	2920	700	3	4.2	3,600	2		1
11 (g)	2920	695	3	4.2	3,600		(a)	
12	2970	685	3.5	4.2	3,600		(a)	
13	2970	670	3.5	4.2	3,600		(a)	
14	2970	665	3.5	4.2	3,600	1		1
15 (h)	2970	655	3.5	4.2	3,600		(a)	
16	2970	645	3.5	4.2	3,600		(a)	
17	2950	630	3.5	4.2	3,600		(a)	
18 (i)	2950	625	3.5	4.2	3,600	2		2
19	2960	540	3.5	4.2	3,600		(a)	
20	2960	540	3.5	4.2	3,600		(a)	
21	2960	545	3.5	4.2	3,600		(a)	
22	3000	555	3.5	4.2	3,600		(a)	
23	3000	570	3.5	4.2	3,600		(a)	
24	3000	575	3.5	4.2	3,600	2		2
25	3000	575	3.5	4.2	3,600		(a)	

12 July 1968--Hemisphere Die⁺--304 Stainless Steel
(Continued)

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Impression No.* 1	2	3
26 (j)	3000	580	3.5	4.2	3,600		(a)	
27	3000	580	3.5	4.2	3,600		(a)	
28 (k)	3000	590	3.5	4.2	3,600		(a)	
29	3000	590	3.5	4.2	3,600	1		2
30 (l)	3000	590	3.5	4.2	3,600		(a)	
31 (m)	3000	590	3.5	4.2	3,600		(a)	
32	3000	600	3.5	4.2	3,600		(a)	
33 (n)	3000	600	3.5	4.2	3,600		(a)	
34	3000	605	3.5	4.2	3,600		(a)	
35	3000	610	3.5	4.2	3,600	2		1
36 (o)	3000	610	3.5	4.2	3,600		(a)	
37 (p)	2950	560	4.5	4.2	3,600		(a)	
38 (q)	2950	565	4.5	4.2	3,600		(a)	
39	2950	575	4.5	4.2	3,600		(a)	
40	3000	570	4.5	4.2	3,600		(a)	
41	3000	575	4.5	4.2	3,600	2		2
42	3000	565	4.5	4.2	3,600		(a)	
43	3000	560	4.5	4.2	3,600		(a)	
44	3000	565	4.5	4.2	3,600		(a)	
45	3030	560	4.5	4.2	3,600		(a)	
46	3030	490	7	4.2	3,600	3		3
47	3030	485	7	4.2	3,600		(a)	
48	3030	485	7	4.2	3,600		(a)	
49	3030	490	7	4.2	3,600		(a)	
50	3030	495	7	4.2	3,600		(a)	
51	3030	495	7	4.2	3,600	3		3
52	2980	495	7	4.2	3,600		(a)	
53	2980	500	7	4.2	3,600		(a)	
54	2980	500	7	4.2	3,600		(a)	
55	2980	500	7	4.2	3,600	2		2
56	2980	500	7	4.2	3,600		(a)	

Comments

For this run, an argon shield was provided over the melt. The runners were widened to 15/16", and the in-gates were increased in thickness to 1/8". The die was sprayed with Glas-Mol 7 and the runners brushed with Arco Perm 100. The ladles were flame sprayed with Al_2O_3; the cracked D5 shot sleeve was employed; and the insides of the asbestos cups were coated with Arco Perm.

Comments (Continued)

+Two impressions only

*1: Least porous; 4: Most porous

(a) Not radiographed
(b) Castings hanging on cracked lip in Impression 1
(c) A small piece of the lip in Impression 1 broke away at Shot 3
(d) Castings hanging on the core in ejector Impression 1 at Shot 5
(e) Biscuit exploded
(f) Large piece of the lip from ejector Impression 1 broke away at Shot 10
(g) Welding occurred at one point on the head of the shot sleeve; the argon cover was observed to have reduced slag formation; slag removal was easy
(h) For Shot 15, soot was applied to the ejector die only; the casting stuck in ejector Impression 1; a piece of the runner that had welded to the head of the shot sleeve had to be chiseled away
(i) Center runner welded to shot sleeve head
(j) Foseco's Slax II added to melt to coagulate slag--no improvement noted
(k) Casting hung in ejector Impression 1; weld spots had to be chiseled from the head of the shot sleeve
(l) After Shot 30, the castings displayed a tendency to crack
(m) Additional cracks noted near the upper end of the D5 shot sleeve
(n) Ejector Insert 3 (wrought molybdenum) had developed a vertical crack
(o) The slag under the argon cover floated on top of the melt and remained easy to remove (The Slax may also have contributed to the ease of removal)
(p) From Shot 37 on, there were no ejection difficulties
(q) The biscuit from Shot 38 exploded

17 July 1968--Hemisphere Die+--304 Stainless Steel

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Impression No.* 1	2	3
1	2980	580	2	4.2	3,600		(a)	
2	2980	590	2	4.2	3,600		(a)	
3	2980	600	2	4.2	3,600	2		3
4	2980	600	2.5	4.2	3,600		(a)	
5	2980	600	2.5	4.2	3,600		(a)	

17 July 1968--Hemisphere Die+--304 Stainless Steel
(Continued)

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Impression No.* 1	2	3
6	2980	605	2.5	4.2	3,600	3		3
7 (b)	2980	600	2.5	4.2	3,600		(a)	
8	2950	590	2.5	4.2	3,600		(a)	
9	2950	580	2.5	4.2	3,600		(a)	
10 (c)	2950	580	2.5	4.2	3,600		(a)	
11	2980	570	2.5	4.2	3,600	2		2
12 (d)	2980	580	2.5	4.2	14,400	2		4
13 (e)	2980	580	2.5	4.2	14,400	3		3
14 (e)	2980	600	2.5	4.2	14,400		(a)	
15 (f)	2980	600	2.5	4.2	14,400	2		3
16	2980	600	2	4.2	14,400		(a)	
17	2980	600	3.5	4.2	3,600		(a)	
18	2980	600	4	4.2	3,600	2		2
19	2980	600	4	4.2	3,600		(a)	
20 (g)	--	600	4	4.2	3,600	2		3
21	--	600	4	4.2	3,600		(a)	
22 (h)	--	600	4	4.2	3,600	2		2
23	--	600	4	4.2	3,600	4		4
24 (i)	--	600	3.5	4.2	3,600	2		3
25 (j)	--	600	4	4.2	3,600	2		2
26	--	600	3.5	4.2	3,600	2		2
27	--	600	3.5	4.2	3,600	3		4

Comments

A new hardened and nitrided H-13 steel shot sleeve was installed for this run, into which a water-cooled beryllium-copper plunger was fitted to a clearance of less than 0.001". The melt was shielded with argon, but as time passed, the slag became heavier and its rate of formation faster. A heavy coating of Arco Perm 100 was applied to the head of the shot sleeve and to the runner area.

+Two impressions only

*1: Least porous; 4: Most porous

Comments (Continued)

(a) Not radiographed
(b) The biscuit made in Shot 7 exploded
(c) At Shot 10, the gate runner cracked, and there was a minor explosion of the biscuit
(d) Shot 12 did not fill Impression 3; the biscuit exploded
(e) The biscuits from Shots 13 and 14 exploded
(f) Shot 15 did not fill the cavities; the biscuit again exploded
(g) A heavy slag cover interferred with optical temperature measurements after Shot 19
(h) Cold shuts in the runners were attributed to changes in the composition of the melt
(i) For Shot 24, the Arco Perm coating on the runners was eliminated; no difficulty with ejection was experienced
(j) From Shot 25 on, both the Arco Perm and the acetylene black were eliminated; the gates were ejected without difficulty

18 July 1968--Hemisphere Die+--304 Stainless Steel

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Impression No.* 1	2	3
1 (b)	2940	600	3.5	4	3,600		(a)	
2	2940	600	3.5	4	3,600	2		2
3	2940	600	3.5	4	3,600		(a)	
4	2940	620	3.5	4	3,600	1		2
5	2940	630	3.5	4	3,600		(a)	
6	2940	630	3.5	4	3,600	2		2
7 (c)	2940	605	3.5	4	3,600		(a)	
8	2940	590	4.5	4	3,600	2		3
9	2950	580	4.5	4	3,600		(a)	
10 (d)	2950	550	5	4	3,600	3		2
11	2950	550	5	4	3,600		(a)	
12	2960	580	5	4	3,600	3		2
13	2960	580	5	4	3,600		(a)	
14	2960	595	5	4	3,600	3		2
15 (e)	--		5	4	3,600		(a)	
16	--	600	5	4	3,600	2		2
17	--	605	5	4	3,600		(a)	
18	--	620	5	4	3,600	3		3
19	--	620	5	4	3,600		(a)	
20	--	625	5	4	3,600	3		3
21 (f)	--	635	5	4	3,600		(a)	
22	--	635	5	4	3,600		(a)	
23	--	635	5	4	3,600		(a)	
24	--	640	5	4	3,600		(a)	

Comments

Heavy die coatings in previous runs had resulted in unsatisfactory surfaces. This run was intended to investigate the effect of various die release agents on the surfaces of the castings.

+Two impressions only

*1: Least porous; 4: Most porous

(a) Not radiographed
(b) The run was begun with Glas-Mol on the die--no soot or Arco Perm
(c) At Shot 7, the gate hung slightly in the die
(d) At Shot 10, a sliver of the runner soldered to the head of the shot sleeve; the head of the shot sleeve and the runner area of the die were coated with Arco Perm 100; Glas-Mol was reapplied to the die
(e) A heavy slag crust on the melt interferred with optical temperature readings
(f) From Shot 21 on, only Aquadag (a suspension of colloidal graphite) was used as a die release agent

23 July 1968--Hemisphere Die+--304 Stainless Steel

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Impression No.* 1	2	3
1	2930	620	2	4	3,600	4		4
2	2930	620	2	4	3,600		(a)	
3	2930	630	2	4	3,600	2		2
4 (b)	2930	645	2	4	3,600		(a)	
5 (c)	2980	650	2.5	4	3,600	1		1
6	2980	660	3	4	3,600		(a)	
7 (c)	2980	670	3	4	3,600	1		1
8	2980	675	3	4	3,600		(a)	
9	2980	680	3	4	3,600	1		2
10	2980	685	3	4	3,600		(a)	
11	2980	660	2.5	4	3,600		(a)	
12	2980	640	3	4	3,600	3		3
13	2980	635	3	4	3,600		(a)	
14	2980	630	3	4	3,600		(a)	
15	2980	630	3	4	3,600	3		3
16	2980	630	3	4	3,600		(a)	
17	2980	630	3	4	3,600	2		2

23 July 1968--Hemisphere Die+--304 Stainless Steel
(Continued)

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Impression No.* 1	2	3
18	2920	625	3	4	3,600		(a)	
19	2920	625	3	4	3,600	4		4
20	2920	620	3.5	4	3,600		(a)	
21 (d)	2920	610	4.5	4	3,600		(a)	
22 (e)								
23	2920	605	5	4	3,600	3		3
24	2960	610	4.5	4	3,600		(a)	
25	2960	610	4.5	4	3,600	2		3
26	2960	610	5	4	3,600		(a)	
27	2960	610	4	4	3,600	2		2
28	2960	610	4.5	4	3,600		(a)	
29	2960	610	3.5	4	3,600	3		3
30	2960	610	4	4	3,600		(a)	
31	2960	610	5	4	3,600	3		3
32 (e)							(a)	
33 (e)							(a)	
34 (e)						4		4
35 (e)						4		4

Comments

For this run, the die was coated with Glas-Mol, over which a suspension of colloidal graphite was swabbed. The asbestos cups were coated with Arco Perm 100. The melt was protected with an argon cover.

+Two impressions only

*1: Least porous; 4: Most porous

(a) Not radiographed
(b) A sliver of the runner welded to the head of the shot sleeve
(c) The radiographs for Shots 5 and 7 indicated that they were nearly perfect; despite the fact that the casting conditions did not change, the results could not be repeated; it was assumed that changes in the melt chemistry, resulting from oxidation, were responsible for the nonreproducibility
(d) Delayed shot
(e) Recorder inoperative

24 July 1968--Hemisphere Die+--304 Stainless Steel

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Impression No.* 1	2	3
1	2950	700	4	4.6	3,600	2		2
2	2950	700	4.5	4.6	3,600		(a)	
3	2950	715	6	4.6	3,600	2		2
4	3000	730	5.5	4.6	3,600		(a)	
5	3000	735	5.5	4.6	3,600	2		2
6	3000	755	5	4.6	3,600		(a)	
7	3000	760	6	4.6	3,600	2		2
8	3050	760	6.5	4.6	3,600		(a)	
9	3050	755	6	4.6	3,600		(a)	
10 (b)	2980	755	7	4.6	3,600	2		2
11	2980	760	6.5	4.6	3,600		(a)	
12	2980	760	7	4.6	3,600	2		2
13	2980	760	6.5	4.6	3,600		(a)	
14	2980	760	6.5	4.6	3,600		(a)	
15	2980	770	6.5	4.6	3,600	2		3
16 (c)	3020	740	7	4.6	3,600		(a)	
17 (d)	3020	740	7.5	4.6	3,600	2		3
18 (e)							(a)	
19	3020	750	6.5	4.6	3,600	1		1
20	3020	760	7.5	4.6	3,600		(a)	
21	3020	760	7	4.6	3,600	3		4
22	2960	760	6.5	4.6	3,600	2		2
23	2960	760	7	4.6	3,600		(a)	
24	2960	760	7.5	4.6	3,600	2		3
25 (f)	2960	760	6.5	4.6	3,600	2		4
26	2960	770	7	4.6	3,600	2		2
27	2960	780	7.5	4.6	3,600		(a)	
28	2960	780	7.5	4.6	3,600	2		2
29	2960	785	7	4.6	3,600	2		3

Comments

This run was made with the die protected by Glas-Mol 7 and a suspension of colloided graphite, which was swabbed on to give coatings of various thickness. For Shots 16, 17, and 18, shims were placed between the die halves on the side of the machine away from the operator, to evaluate the influence on porosity of additional venting. Neither spitting nor excessive flash were observed; ejection was not a problem.

+Two impressions only

*1: Least porous; 4: Most porous

Comments (Continued)

(a) Not radiographed
(b) Center runner hung in die
(c) 0.010" shim inserted
(d) 0.020" shim inserted
(e) 0.030" shim inserted--recorder inoperative
(f) High plunger velocities, in the range of 6 to 7.5 ips, and higher die temperatures did not, contrary to expectation, improve the quality of the castings

25 July 1968--Hemisphere Die[+]--Malleable Iron[++]

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Impression No.*		
						1	2	3
1	2640	695	4	4	3,600	2		2
2	2640	705	4.5	3	3,600		(a)	
3	2640	715	4.5	3	3,600	2		1
4	2640	725	4.5	3	3,600	2		1
5	2640	725	4	3	3,600		(a)	
6	2650	735	4	3	3,600		(a)	
7	2660	755	5	3	3,600		(a)	
8	2660	760	4.5	3	3,600		(a)	
9(b)(c)	2740	760	5	3	3,600	4		2
10	2740	760	5	3	3,600		(a)	
11	2740	740	5	3	3,600		(a)	
12	2740	720	5	3	3,600	4		3
13	2740	700	5	3	3,600		(a)	
14	2800	590	6	3	3,600		(a)	
15	2720	560	5	3	3,600	3		3
16	2720	550	5	3	3,600		(a)	
17	2640	550	5	3	3,600	2		1
18	2640	560	6	3	3,600		(a)	
19	2660	560	6	3	3,600		(a)	
20	2660	610	5	3	3,600	3		2
21	2660	615	5	4.1	3,600		(a)	
22	2660	625	6	3.9	3,600		(a)	
23	2720	635	5.5	4	3,600	3		4
24	2680	640	5.5	2.7	3,600		(a)	
25	2660	700	7	2.7	3,600	3		4

25 July 1968--Hemisphere Die+--Malleable Iron++

(Continued)

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Impression No.* 1	2	3
26	2660	700	7	3	3,600	3		4
27	2660	720	7	3	3,600	3		4
28	2680	720	7	3	3,600		(a)	
29	2690	740	7	3	3,600	2		4
30 (c)	2690	740	7	2.5	3,600	1		1
31	2690	740	6.5	2.5	3,600		(a)	
32	2690	740	5.5	2.4	3,600		(a)	
33	2670	750	5.5	2.2	3,600		(a)	
34						1		1
35	2670	750	5.5	2.2	3,600		(a)	
36	2670	750	7	2.2	3,600		(a)	
37	2660	750	7	2	3,600	1		3
38	2660	755	7	2	3,600		(a)	
39	2660	755	7	2	3,600	2		2
40	2680	740	7	2	3,600	4		1
41	2680	740	6	2	3,600	4		3
42	2680	740	6	2	3,600		(a)	
43	2680	750	7	2	3,600	3		3
44	2680	750	6	2	3,600		(a)	

Comments

The cover die and the area of the ejector die opposite the plunger were coated with Glas-Mol for this run; acetylene black only was applied to remainder of the ejector die. The first 24 shots were made with stainless steel ladles coated with WRP-X felt.

+Two impressions only

++CE=2.80% It was found quite difficult to maintain a constant carbon equivalent. (It may be suggested, in retrospect, that this difficulty was related, in part, to an unnecessarily high melt temperature.)

*1: Least porous; 4: Most porous

(a) Not radiographed
(b) One runner broke in the die on ejection
(c) The first four shots were notably superior to the next 25 shots; a reduction in porosity was again noted for Shots 30 to 34 or 37; these variations were attributed to changes in the melt chemistry with time, which were countered by periodically removing the slag and by making occasional additions to the melt to adjust the chemistry

26 July 1968--Hemisphere Die+--304 Stainless Steel

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Impression No.* 1	2	3
1	2990	710	3	2	3,600	2		2
2	2990	715	4	2	3,600	2		2
3 (b)							(a)	
4	2990	715	--	2	3,600	1		3
5 (c)	2990	720	5.5	2	3,600	2		2
6 (d)	2990	725	6	2	3,600	2		2
7	2990	740	6	2	3,600	2		3
8 (e)	3060	730	6.5	2	3,600	2		2
9	3040	740	6.5	2	3,600	2		3
10	3040	740	5.5	2	3,600	4		4
11	3040	730	3.5	2	3,600	3		3
12	3040	750	3.5	2	3,600	3		3
13	3040	750	3.5	2	3,600	3		3
14	3000+	750	3.5	2	3,600	3		3
15	3000+	750	3	2	3,600	4		4
16	3000+	755	3	2	3,600	4		4

Comments

For this run, Arco Perm 100 was applied to the runner areas of the die.

+Two impressions only

*1: Least porous; 4: Most porous

(a) Not radiographed
(b) Recorder inoperative
(c) The runner to Impression 3 broke on ejection after Shot 5
(d) The runner to Impression 1 broke and the casting stuck to the core in Impression 1 after Shot 6
(e) Another runner broke on ejection after Shot 8
(f) Shot 9 also produced a casting which stuck to the core in Impression 1
(g) After Shot 10, the ejector pins seized

14 August 1968--Hemisphere Die+--Malleable Iron

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Impression No.* 1	2	3
1 (b)	2660	710	3.5	2	4,000		(a)	
2 (c)	2640	745	3	2	4,000		(a)	
3 (d)	2660	760	3.5	2	4,000	1	3	1
4	2680	770	4	2	4,000	1	2	2
5 (e)	2740	780	3	2	4,000		(a)	
6 (f)	2740	800	2	1.4	4,000		2	
7 (g)	2740	720	4	3.6	4,000	2	2	2
8	2740	760	4.5	3.6	4,000		(a)	
9 (h)	2740	790	4.5	3.6	4,000	1	2	2

Comments

+For this run, Impression 2 was reactivated with a new, high-density, pressed and sintered molybdenum cover impression block, making the hemisphere die again a three-impression die.

*1: Least porous; 4: Most porous

(a) Not radiographed
(b) The gate broke on ej^ction after Shot 1
(c) After Shot 2, the asbestos cups were all coated with Arco Perm 100
(d) The gate from Shot 3 stuck in the biscuit area and hung on the head of the shot sleeve
(e) Shot 5 produced a very thin biscuit; the gate broke on ejection
(f) The biscuit from Shot 6 exploded (note dwell time), and one of the runners again welded to the head of the shot sleeve; only the center cavity filled
(g) The die was recoated with Glas-Mol for Shot 7; one of the runners broke on ejection
(h) Shot 9 produced a complete gate, intact; a leak was discovered in a water line which forced the run to be terminated

16 August 1968--Hemisphere Die+--Malleable Iron++

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Impression No.* 1	2	3
1 (b)	2700	560	3.5	3.7	4,000		(a)	
2	2680	560	3.5	3.7	4,000	4	1	3
3	2680	620	5	3.8	4,000		(a)	
4	2680	620	4	3.8	4,000	4	4	4
5 (b)	2700	630	4	3.8	4,000		(a)	
6	2700	640	5	3.8	4,000	4	1	4
7	2700	640	5	3.8	4,000		(a)	
8	2700	600	5.5	3.8	4,000	4	2	4
9	2700	640	5.5	3.8	4,000		(a)	
10 (b)	2700	660	6	3.8	4,000	4	2	4
11	2700	670	6	3.8	4,000		(a)	
12	2700	650	7	3.8	4,000	4	2	4
13	2700	650	5	3.8	4,000		(a)	
14	2700	650	5	3.8	4,000	4	2	4
15	2700	640	6.5	3.8	4,000		(a)	
16	2700	680	6.5	3.8	4,000	4	3	4
17	2700	640	7	3.8	4,000		(a)	
18	2700	645	7.5	3.8	4,000		(a)	
19	2700	630	7.5	3.8	4,000	4	2	4
20	2700	650	7	3.8	4,000		(a)	
21	2700	650	8	3.8	4,000	4	2	4
22	2700	610	6.5	3.8	4,000		(a)	
23 (c)	2700	610	8.5	3.8	4,000		(a)	
24 (c)	2700	610	5.5	3.8	4,000	4	4	4
25	2700	650	7.5	3.8	4,000		(a)	
26	2700	600	7	3.8	4,000	3	3	--
27	2700	600	7.5	3.8	4,000		(a)	
28	2700	640	6.5	3.8	4,000	4	2	4
29	2700	640	7	3.8	4,000		(a)	
30 (d)	2700	690	8	3.8	4,000	4	2	4
31	2700	670	8.5	3.8	4,000		(a)	
32	2700	680	8.5	3.8	4,000		(a)	
33 (e)	2700	700	8	3.8	4,000		(a)	
34	2700	620	7.5	3.8	4,000		(a)	
35	2700	610	7.5	3.8	4,000	4	2	--

16 August 1968--Hemisphere Die+--Malleable Iron++
(Continued)

Shot No.	Metal Temp (°F)	Die Temp (°F)	Plunger Speed (in/sec)	Dwell Time (sec)	Metal Pressure (psi)	X-ray Impression No.* 1	2	3
36	2760	615	7.5	3.8	4,000		(a)	
37	2760	615	7.5	3.8	4,000	4	1	--
38	2760	615	7	3.8	4,000		(a)	
39	2760	615	7.5	3.8	4,000	2	2	--
40	2760	625	7.5	3.8	4,000		(a)	
41	--	630	7.5	3.8	4,000	2	1	4
42	2730	670	7.5	3.8	4,000		(a)	
43	2750	690	7.5	3.8	4,000		(a)	
44	2770	680	5	3.8	4,000		(a)	
45 (f)	2770	690	5	3.8	4,000	2	2	3
46 (g)	2770	690	5	3.8	4,000		(a)	
47	2740	690	4.5	3.8	4,000	2	1	3

Comments

+Three-impression configuration

++CE = 2.67%

*1: Least porous; 4: Most porous

(a) Not radiographed
(b) Glas-Mol was applied to the die before Shots 1, 5, and 10
(c) Castings from Shots 23 and 24 stuck in cover Impression 3
(d) From Shot 30 through Shot 45, WRP-X cups were substituted for asbestos cups
(e) Shot 33 welded in cover Impression 3; the carbon equivalent was redetermined; it was 2.7%
(f) From Shot 45 on, the biscuits began to weld in the ejector die and had to be removed by chiseling
(g) Shots 46 and 47 were made with asbestos cups coated with Arco Perm 100

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13. ABSTRACT

To demonstrate that a device in the form of a hemisphere could be produced by ferrous die casting at a lower cost than by aluminum die casting, the desirability and feasibility of automatically transferring molten ferrous alloys to an injection system was explored. The requirements of an injection system were established; materials of construction and design concepts were selected and tested for both vertical and horizontal die casting machines. New die design concepts were explored. Operational results are reported. The structures and properties of several die cast ferrous alloys were characterized, and 16 potential die materials were evaluated. Prototypes were fabricated, from which the final material and configuration for the hemispheres could be selected. A die was designed and fabricated for pilot production, and the results of that pilot casting operation are reported. The cost of the pilot castings was analyzed and potential costs were projected. Recommendations are made for additional support.

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